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VOLUME 11, NUMBER 7 • SEPTEMBER, 1955

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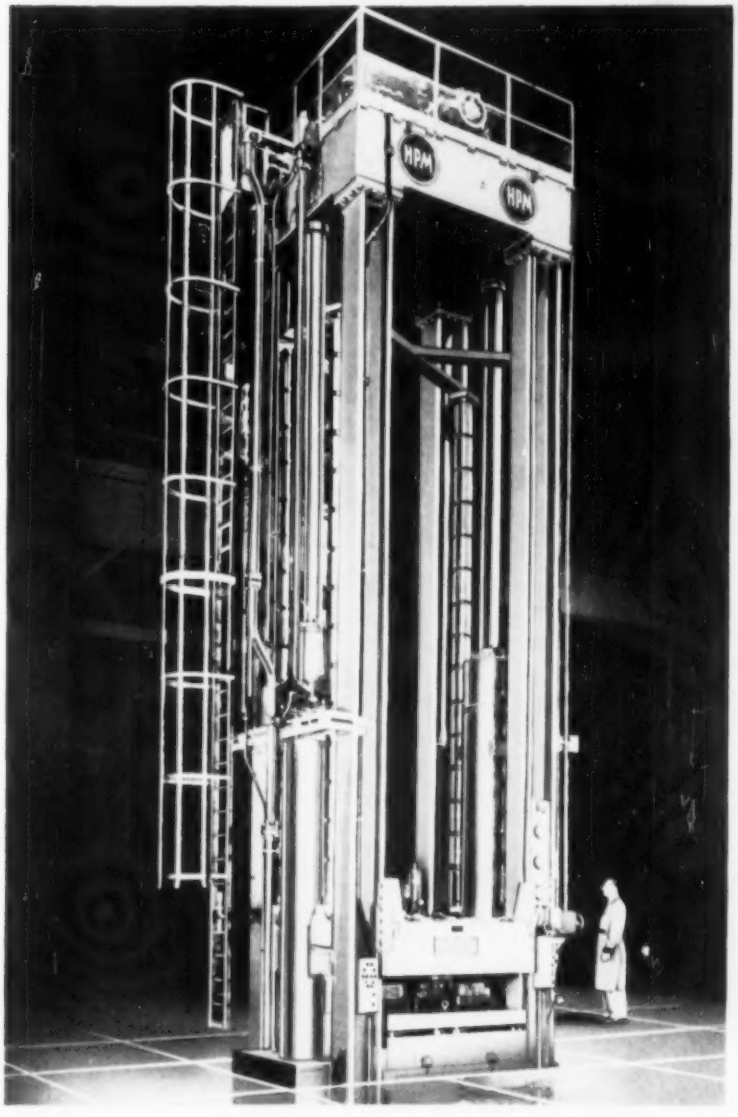

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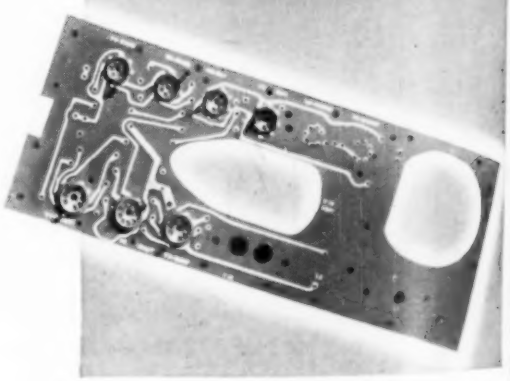

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Vol. 11 September, 1955 No. 7

Published monthly except

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• *All copy intended for publication in the SPE Journal should be directed to the Editorial Offices. Copy for each issue should be received at Athens, Ohio, before the first of the preceding month. Neither The Society of Plastics Engineers nor the SPE Journal is responsible for the views expressed by individual contributors.*
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SPE JOURNAL

Vol. 11 September, 1955 No. 7

Published monthly except July and August by

THE SOCIETY OF PLASTICS ENGINEERS, INC.

30 cents a copy $3.00 per year

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Dave Sloan (front, left) showing an injection machine part in process of construction to Elmer Faber, G. R. Bennett, Dick Serrick and Erik Kohler at a recent meeting of the Toledo Section held in Cleveland.

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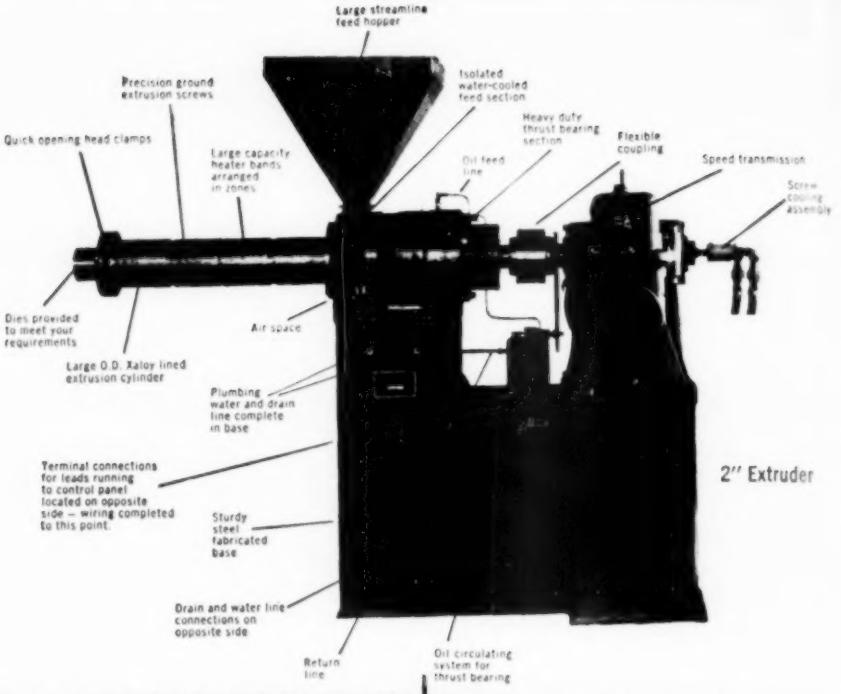
Egan plastic extruder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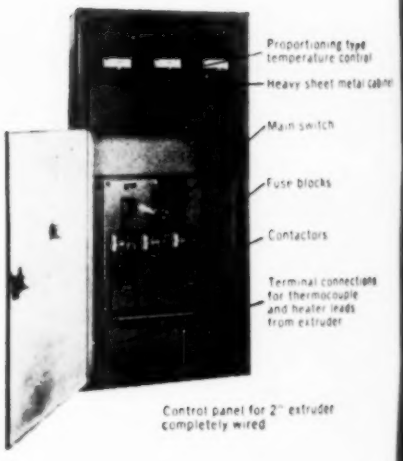

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Maximum Heating Load, KW	12	18	30	48	96	160
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Type Speed Transmission	Worm gear.	Worm gear.	Herr' bone	Herr' bone	Herr' bone	Herr' bone
Usual Motor Size, H. P.	7½-10	10-15	20-30	40-50	50-75	75-150
Approx. Extrusion Capacity lb./hr.	20-50	40-90	75-200	150-450	250-650	450-1200
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Approx. Weight—lbs. (less Drive)	3200	3600	4800	6600	9600	12500

**Extruders are modified to meet requirements for a particular process*

Twelfth SPE Conference To Be Held in Cleveland

The 12th annual Conference of the Society of Plastics Engineers will be held in Cleveland, Ohio at Hotel Statler in 1956, according to a statement made by Frank A. Martin, general chairman of the Executive Committee. The conference extends from Tuesday, January 18 through the 20th.

Approximately 2,000 members of SPE are expected to attend the technical conference. Registration will begin Tuesday afternoon, January 17, with talks starting Wednesday morning and extending through Friday. The annual business meeting and luncheon will be held Wednesday, and a banquet is scheduled for Thursday evening.

Subjects to be covered during the meeting are these phases of plastics engineering:

Injection Molding, Technique and Machinery; Compression and Transfer, Technique and Machinery; Extrusion, Technique and Machinery; Calendering and Casting, Technique and Machinery; Reinforcement (by fibers, cloth—by elastomers); Foams; Professional Activities and Education; Physical Properties (Methods, Comparative, Correlations, Creep); Research; Defense Applications of Plastics; Automotive and Aircraft Applications; Rigid Plastics; Application and Processing; General or Unassigned.

Papers on the above subjects have been approved and preprints on these discussions will be given to each registrant.

Details of the meeting are being arranged by the Executive Committee and special committee chairmen. The following men compose these committees:

General Chairman, Frank A. Martin, The Hoover Company; *Co-Chairman*, Sherman W. Crawford, Detroit Mold Engineering; *Treasurer*, Ernest P. Moslo, Moslo Machinery; *Secretary*, Jack Pectal, Eastman Chemical Sales.

Committee Chairmen: *Speakers*, William E. Manring, B. F. Goodrich Chemical Company; *Program*: Hugh Winn, Firestone Tire and Rubber Company; *Registration*, Harry Zimmerman, B. F. Goodrich Chemical; *Publicity*, Samuel A. McElroy, Hercules Powder; *Sergeant at Arms*, R. B. Treer, Warner and Swasey; *House*, Harold Olson, Harshaw Chemical; *Printing*, A. R. Morse, Injection Molders Supply; *Advertising*, Barney Wolfe, Injection Molders Supply; *Banquet*, Kenneth M. Keller, Keller Products; *Company*, R. C. Lopez, H. Muelstein; *Advance*, H. D. Oliver, Jr., Eastman Chemical Sales; *Ladies*, Miss Elizabeth Goodine, Geauga Industries; *Prizes*, A. Edward Campi, International Molded Plastics; *Finance*, Ernest P. Moslo, Moslo Machinery.

Rooms and suites are available at the Hotel Statler. During the meeting, tours and fashion shows will be planned for the wives of SPE members.

For further information please contact S. A. McElroy, Hercules Powder Company, 865 Union Commerce Bldg., Cleveland, Ohio, SUperior 1-4335.

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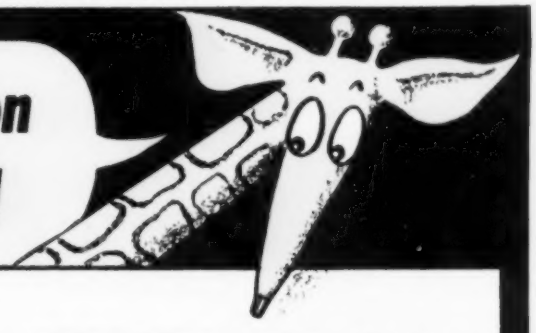

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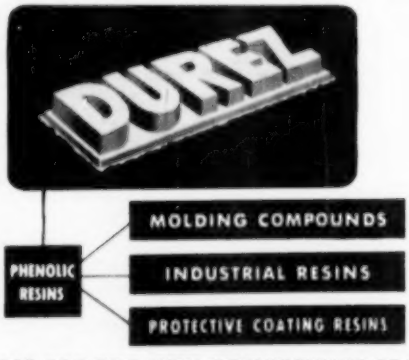

PHENOLIC PLASTICS FOR THE NEW COMPETITIVE ERA

Book Reviews . . . .

Chemical Business Handbook, prepared by a staff of specialists, John H. Perry, Editor in Chief. Over 1,300 pp. $17.00 McGraw Hill Company, Inc., New York, First Edition, 1954.

124 authors contributed to this book under the editorship of John H. Perry, editor of *Chemical Engineers Handbook*, who unfortunately died while this book was in the press.

It is divided in 20 sections which have each their own pagination. The first three sections deal with business, finance, management and control and commercial chemical development. They contain selected financial and business data, describe accounting methods and marketing and development procedures. (Many readers will object to Carl Pacifico and Thomas H. Vaughn that they do not make any difference between commercial laboratories and professional consultants.) The following three sections are devoted to research. Section 6 contains 282 pages of valuable data and sources of information on market research such as annual production of a great many chemicals, use of these chemicals, consumption data and market research.

The commercial aspects of chemical manufacturing are treated in sections 7 to 15: purchasing, production, transportation, sales, advertising, collections, personnel management, public relations and business law. These chapters initiate the technical man and the executive to management in the office, the plant, the laboratory and in public relations. The following sections are highly

(Please turn to page 8)

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Book Reviews . . .

(Continued from page 6)

specialized. Section #16 deals with patents and patent laws and certainly belongs in a book of that kind. Section #17 is devoted to industrial toxicology and gives in a few pages the elements of internal and external toxicity, toxicological investigations and tests which all chemists should know. Section #18 concerns insurance and loss prevention and will be of interest particularly to plant managers and heads of personnel departments. The last two sections contain reports, report writing and business mathematics. They give data which will greatly help executives dealing with reporting, operations research and statistics.

It is obvious that this handbook will be indispensable to beginners in the chemical business. Furthermore, we believe it will be of great value also to technical men and executives in the many problems which confront them in their diversified duties. This encyclopedia of outstanding data, some of them not readily available elsewhere, should be on every chemist's desk.

Dr. Louis C. Barail

PLASTIC TOOLING, Malcolm W. Riley; Reinhold Publishing Corporation, New York, 123 pages.

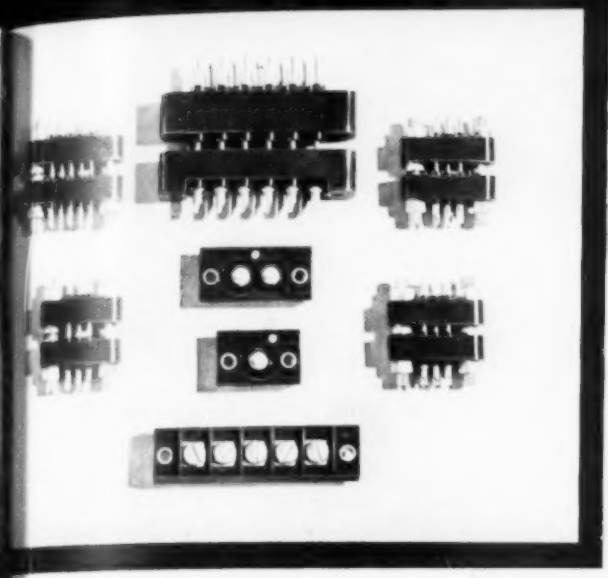
This is a Reinhold Pilot Book, written to give a concise and timely presentation of the growing plastic tooling field.

The book deals primarily with methods, materials, and applications of plastic tooling in industry. Fundamentals, economic aspects, and properties are briefly discussed.

This book can properly be termed a handy reference book for engineers, shop personnel, and those contemplating entering this field or using plastic tools. The author takes the stand that plastic tooling is not a substitute for metal tooling but proven alternate materials, useful in their own right, with definite possibilities, limitations, advantages, and disadvantages. This thinking is reflected throughout the text which covers such subjects as tooling resins, fabricating, checking fixtures, assembly fixtures, metal forming tools, plastic forming tools, die models, and prototypes. A chapter is also devoted to the future of experimental tools. The book has 10 chapters in all, together with a number of pictures and illustrations.

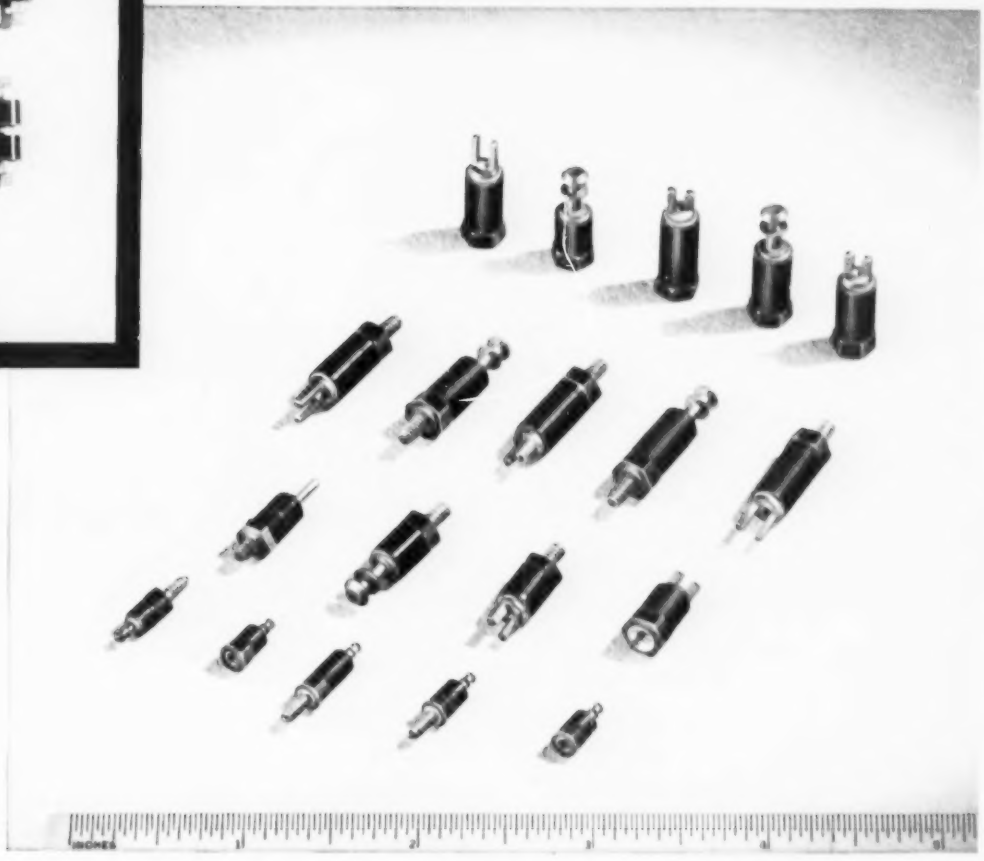
Ronald D. Beck

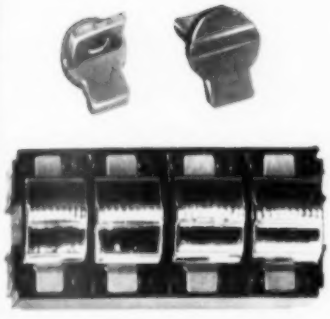
MOLD AND DIE MAKING FACILITIES: Bulletin describes the more than 150 pieces of capital equipment including grinders, milling machines, duplicators, lathes, presses, etc., this firm used in producing and testing custom molds, dies and hobs for plastics processors. Newark Die Co., 20 Scott St., Newark, N. J.



Multi-contact connectors molded of Cyanamid's Melamine No. 592 by Armel Electronics, Inc., Brooklyn, N. Y.

Standoff terminals molded of mineral-filled Melamine No. 592 by Whitso, Inc., Schiller Park, Ill.





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George J. Geretschlaeger, Minnesota Plastics Corporation, St. Paul, Minn. For 15 years Mr. Geretschlaeger has been learning his craft at every operational level. Today, as Molding Superintendent, he applies his first-hand knowledge of molds and materials to every job. Recently, by designing a trimming fixture that did away with hand de-gating, he stepped up production on a washer for casement windows from 2,500 to 4,500 pieces an hour. Says Mr. Geretschlaeger: "It takes the combined talents of many people with long experience in the various phases of injection molding to produce quality plastic parts at minimum cost."

Fred J. Kirk, Kirk Molding Co., Clinton, Mass. A background of automotive and aircraft skills has shaped Mr. Kirk's philosophy that "producing the best products at lowest cost is a matter of exacting the best performance from machines and materials." As his company's owner, he has won customer confidence by equipping his plant with some of the finest automatic equipment available in the industry. Specializing in custom molded packages for products such as silverware and precision power tool components, Mr. Kirk contributes his personal attention and 14 years' experience in injection molding engineering to every job.

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"The objects of the Society shall be to promote in all lawful ways the Arts, Sciences, and Engineering Practices and Standards connected with the utilization of plastics."

PAC STEPS FORWARD

by Don F. Hoffman, PAC Administrator

After fourteen years of struggle for the organization of the membership into groups of specific branches of plastics engineering* the Society is now reaching toward success through the work of PAC. Progress has already been made, but contributions to the work of PAC by every SPE member are necessary to obtain the objective.

The overall organization of PAC was presented to you in chart form in the May '55 SPE JOURNAL. At the June PAC Meeting in Minneapolis additions to the organizational chart and some revisions in terminology were made.

The full name of PAC is now the Professional Activity Committees.

Miscellaneous projects under Mr. Jerome Formo have been reclassified as Special Technical Projects.**

Geographical names have replaced the numerical system of the PAC Engineering Volumes Steering Committee as follows:

Region I	—New England	Region V	—Midwest
Region II	—St. Lawrence	Region VI	—Gulf
Region III	—Atlantic	Region VII	—Pacific
Region IV	—Ohio-Pittsburgh	Region VIII	—Central

Mr. John Delmonte has been named Chairman of the Pacific Region Engineering Volumes Steering Committee.

It is expected that membership groups of specific branches of plastics engineering will be developed as a concurrent result of the work now started by the PAC Technical Volumes Steering Committee.

At the June meeting of this committee work was assigned for re-editing the book, "Injection Molding." Chapter assignments have been made to the eight PAC regions as listed. Any qualified member desiring to help re-edit this book is requested to contact his Section's National Director.

Chapter 1 — History, New England. Chapter 2 — Development of Injection Molding Techniques, New England. Chapter 3 Materials for Injection Molding, Atlantic, Ohio, Pittsburgh. Chapter 4 — Molding Practices, Atlantic and Midwest. Chapter 5 — Product Design, Pacific. Chapter 6 — Mold Design and Construction, Central. Chapter 7 — Finishing and Decorating, Midwest. Chapter 8 — Estimating and Costs, Gulf. Appendix I and II Physical properties and general data, St. Lawrence.

"Injection Molding" is scheduled for republishing June, 1956.

It has been decided to point this and all PAC technical publications to the professional or post graduate level so as to make these works of interest particularly to engineers and other technical personnel.

Regional editors for this publication are being named and a full report is expected at the September 12 meeting of the Steering Committee in Cleveland.

The Technical Project Committee on Quality Control, Dr. L. Debing, Chairman, has completed its assigment of preparing and publishing a complete series of papers on the use of quality control in plastics. The next step in this committee's work will be to publish the papers in a collected volume with whatever minor re-editing may be required for this purpose. It is the committee's hope to have this published late this year.

The Cooperative Technical Programs Committee under J. H. DuBois has made assignments to members of SPE who will act as our official representative in work done by other technical societies. It is expected that the complete list of liaison men will be published in the very near future.

Recent acceptances by ASTM Committee D-20 on Plastics of SPE Representatives are:

Name	SPE Section	ASTM Subcommittees
N. L. Perry	New York	V, XV, XVIII, XIX
H. H. Dash	New York	VIII, X, XVIII
E. N. Dorman	New York	Not selected as yet
F. J. Donohue	Western New England	VII, IX, XVI
C. F. Massopust	Chicago	Not selected as yet

SPE's liaison member with the International Standards Organization, Dr. Norman Skow of the Philadelphia Section, attended the ISO/TC61 Plastics Committee meeting in Paris July 8 to 13.

Dr. Skow reports that sixty-eight delegates were in attendance representing the following countries: United States, United Kingdom, France, Germany, Switzerland, India, Sweden, Belgium, Italy, Netherlands, and Czechoslovakia.

The U. S. was represented by Dr. G. M. Kline of the National Bureau of Standards, Mr. Robert Burns of the National Research Council, Dr. Norman A. Skow of the Synthane Corporation, Mr. W. A. Frantz of E. I. duPont, Mr. R. R. Winaus of the New York Naval Shipyard, Mr. E. Y. Wolford of the Koppers Company, Mr. C. H. Adams of Monsanto Chemical Company, Mr. J. W. McNair of the American Standards Association, Dr. R. K. Witt of Johns Hopkins University and Mr. E. E. Ziegler of Dow Chemical Company. Dr. Kline served as President of the meeting and Dr. Skow as Secretary. Mr. Burns was the Leader of the U. S. delegation.

Plenary meetings were held at the beginning and end of the five day session but most of the time was devoted to smaller meetings of the eight working groups. The working groups covered the following problems:

Working Group No. 1 — Nomenclature & Definitions

Working Group No. 2 — Mechanical Strength Properties.

Working Group No. 3 — Standard Laboratory Atmospheres and Conditioning Procedures

Working Group No. 4 — Thermal Properties

Working Group No. 5 — Physical Chemical Properties

(Please turn to page 12)

*SPE Constitution Article XII — Professional Activity Groups

**SPE Constitution Article XIII — Technical Committees

National Organization . . .

(Continued from page 11)

Working Group No. 6—Ageing, Chemical & Environmental Resistance

Working Group No. 7 — Preparation of Test Specimens

Working Group No. 8 — Review of Methods for Electrical Properties.

A more complete report on the actual accomplishments of these working groups will be made in the future. Dr. Skow reports that it was gratifying to see how diligently the members of the working groups tackled their problems and to observe the spirit of genuine cooperation. When their work is finally complete it will lead to more friendly relationships between the various countries as well as to a better understanding of the products sold in inter-country trade.

The meeting ended in a mutual feeling that a difficult problem is well on its way to solution.

Liaison work is anticipated with other societies such as American Society of Safety Engineers, National Society of Corrosion Engineers, American Association of Mechanical Engineers, Manufacturing Chemists Association, SPI, Building Research Institute, American Standards Association, and many others.

Regional Technical Conferences — This also is a new committee. Established at PAC's June meeting, it shows promise of fast progress.

(This report will be continued in the October issue.)

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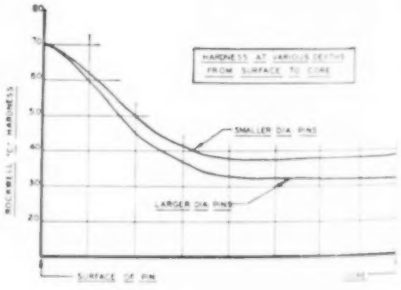

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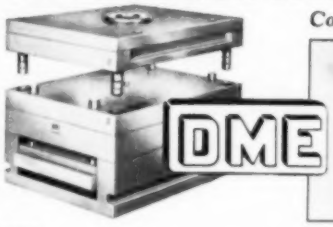

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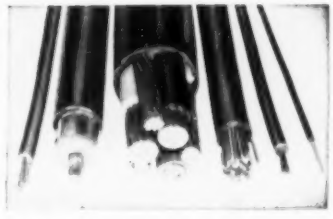

Jacketing and insulation of "Alathon" for wire and cable have excellent dielectric properties, superior abrasion and heat resistance, and excellent weatherability.

MECHANICAL PROPERTIES		Units	Test method	
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	73 °F.	lb./sq.in.	D412-51T	1,750
	158 °F.	lb./sq.in.	D412-51T	850
	−70 °F.	lb./sq.in.	D638-52T	4,750
	73 °F.	lb./sq.in.	D628-52T	1,900
	158 °F.	lb./sq.in.	D638-52T	950
Yield point	−67 °F.	lb./sq.in.	D412-51T	4,800
	73 °F.	lb./sq.in.	D412-51T	1,700
	158 °F.	lb./sq.in.	D412-51T	850
Elongation	−67 °F.	%	D412-51T	60
	73 °F.	%	D412-51T	600
	158 °F.	%	D412-51T	600
Modulus of elasticity	73 °F.	lb./sq.in.	D638-52T	35,000
Stiffness	−67 °F.	lb./sq.in.	D747-50	230,000
	73 °F.	lb./sq.in.	D747-50	27,000
	158 °F.	lb./sq.in.	D747-50	8,000
Shear strength	73 °F.	lb./sq.in.	D732-46	2,750
Impact strength, Izod	−40 °F.	ft.lb./in.	D256-47T	0.8
	73 °F.	ft.lb./in.	D256-47T	did not break
Hardness, Shore			D676-49T	C81,D48

THERMAL PROPERTIES			
Thermal conductivity	B.T.U./hr./sq.ft./°F./in.	Cenco-Fitch	1.75
Heat distortion temperature, 66 lb./sq.in.	°F.	D648-45T	121
Softening point, Vicat	°F.		201
Brittleness temperature	°F.	D746-51T	<−105

ELECTRICAL PROPERTIES	Units	Test method	
Dielectric constant 60 cycles to 100 megacycles,		D150-47T	2.3
Power factor, 60 cycles to 100 megacycles		D150-47T	0.0005

MISCELLANEOUS PROPERTIES			
Flammability	in./min.	D635-44	0.94
Specific gravity		D792-50	0.92
Water absorption	%	D570-42	0.014
Melt index		D1238-52T	2.1
Index of refraction	n_D	D542-50	1.51
Mold shrinkage	in./in.		0.02-0.05
Compression ratio		D392-38	2.1–3.6

NOTE: Data shown are average values of molded "Alathon" [1]. These values should not be used for specifications.

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N. Y. Reinforced Meeting

Joel Frados

The Reinforced Plastics Group of SPE's New York Section held its regular dinner meeting on May 25 at Tufaro's Restaurant, Corona, with 57 members and guests attending. Robert J. Brinkema, Brinkema Plastics, served as moderator of a five-man panel on "Tool Design in Molding Reinforced Plastics."

John C. Moricoli, C. L. Gougler Machine Co., opened the panel talks with a discussion of "Machining of Matched Metal Molds for Molding of Reinforced Polyester Resins." He outlined six basic steps imperative for high quality mold making: pre-design planning, mold design (layout), approval of mold design by the molder, purchasing of mold steels, detailing of mold design, and pre-machining conferences between engineering and production. Mr. Moricoli also made recommendations concerning steels, hardening of molds, chrome plating, finishes, tolerances, shear edges, and standardization of inquiries.

"Cast Aluminum Tooling" was the subject of a talk by John Avignone, M. A. Cuming & Co. Differential expansion in steel and aluminum limits molds designed from the latter material to the resin seal type, straight compression, or a landed mold. Heating and cooling advantages of cast-in tubing can be utilized in aluminum castings due to the metal's high conductivity. Molds should have uniform wall thickness with additional strength possible through rib constructions and annealing. Ease of machining and lightness of weight are reported economy features.

The Kellering process was outlined by Milton Hoffman, Victor Kellering, Inc. Victor Kellering machines are used as automatic tracers for controlled molding and work on an electrical principle. Close tolerances (five mils) on material thickness are possible; however, speed is difficult to obtain. The process is used essentially to obtain accurate matched metal molds, and is not intended to compete with the two aforementioned methods.

"Design of Matched Metal Molds from the Molder's Viewpoint" was presented by Sidney D. Yarm, Bassons Industries Corp. He listed the following considerations as essential to mold design: quantity of parts required, part size, complexity of part, surface finish desired, whether trimming and blanking are to be performed in the molding operation, likelihood of design changes, and the type of delivery schedule involved. Advantages of aluminum, cast iron, Meehanite, and steel molds were considered. A major difficulty encountered by the molder in working the molds is the securing of proper draft; one degree minimum being required.

Frank Nussbaum, Riverside Plastics Corp., described bronze molds as releasing well without inhibiting the cure. Expensive, they best serve in molding situations requiring intricate shapes and indefinite wear.

Mold release problems with steel, bronze, and aluminum were considered by the panel, and the following conclusions reached: after polishing, chrome plating, and buffing serve well for parts requiring painting; porosity is limited by impregnating the mold with a parting agent having a non-aqueous base; and molds are best cleaned with paper which is immediately thrown away.

Deflashing Bulletin Available

Deflashing of thermosetting molded plastics at high speeds by means of automatic methods is discussed in a new bulletin just published by American Wheelabrator & Equipment Corp., 1258 S. Byrkit St., Mishawaka, Indiana.

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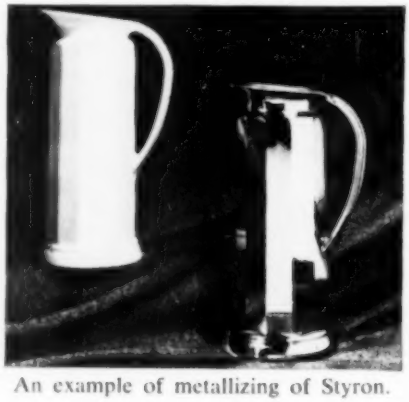
First, of course, you consider the results you want. Then you apply the coating that will give you that result. It is important, however, to pay careful attention to application technique. For example, lacquers are used for the striking back painting method which gives depth and gem-like appearance to escutcheons. But to be just right, the molded part should be checked for residual stress and the effect of lacquer solvent on crazing. There must be enough solvent attack to insure lasting adhesion but not enough to weaken the molding. It is important to know whether lubricants are used in the material or molding process and what effect they may have on surface adhesion. Surface treatment and application techniques can be worked out to meet specific needs. For instance, metallizing can be accomplished in any one of five different ways to provide additional physical properties or improved appearance.



An example of metallizing of Styron.

For a specialized interior finish to resemble suede, felt or mohair, a flocking process may be used. Metallizing, flocking and lacquering require many of the same basic considerations when it comes to proper application techniques. Printing and labeling to add printed words, identification, directions, etc., also pose application problems similar to lacquering, although not as critical in nature. Of course, every different type of application technique, coating, or finish requires special attention to its own problems.

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Dow studies can be of service to you because they include extensive information not only on different types of coatings and finishes but on proper application techniques, surface cleaners, drying procedures and other data. This information can help you produce results while avoiding a long trial-and-error approach that often means a costly expenditure in both time and money.

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For more complete information on this subject, send for your free copy of Dow's Technical Service bulletin entitled "Coatings and Finishes for Styron." Write to Plastics Sales Department PL. 455Z-2, THE DOW CHEMICAL COMPANY, Midland, Michigan.

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Rheology Issue

This issue of the Journal has papers dealing with the single topic of rheology. It is frankly an experiment, which might be repeated annually if there is enough demand for it. In this issue we also strayed afield in one article from plastics, in the hope that there is something to learn in allied fields. We invite your comments on the notion of an annual rheology issue.

When the Society of Rheology convened for its Annual Meeting in November, 1954, at the National Bureau of Standards in Washington, the turnout of papers and the attendance were certainly assisted by the attractions offered by the meeting place, today's most important center of fundamental rheological research in the country; but at the same time it was a sign of the increasing awareness of Science and Technology of what Rheology, the molecular-physical-mathematical approach to the deformation of matter, had to offer.

The large number of papers given presented our Society with a serious publication problem. Traditionally, these papers are collected in a Rheology Issue of a host-Journal, for some time the Journal of Colloid Science and for the last two years the Journal of Applied Physics. This year, the printing space required definitely exceeded even the most generous allowance that any one journal could grant in fairness to its other contributors. We were therefore most fortunate in obtaining additional space in the Society of Plastics Engineers Journal, so that we will be able to distribute two Rheology Issues this year.

Notwithstanding this favorable development, the absence of a single comprehensive volume will be regretted by some. In order to maintain the coherence of the meeting to a certain extent, the Editor has kindly agreed to print below the titles of the papers which will appear in the companion issue of the Journal of Applied Physics, as well as of those authors who were under different publication obligations. Abstracts of all papers have appeared collectively in the Fall Issue of the Rheology Bulletin, 1954.

Papers which will appear in the Rheology Issue of the Journal of Applied Physics, July 1955:

"Viscosity and Elasticity of Oil Surfaces and Oil-Water Interfaces"
Dean W. Criddle and Arthur L. Meader
California Research Corporation, Richmond, Cal.

"Application of the Superposition Principle and Theories of Mechanical Equation of State, Strain- and Time-Hardening to Creep of Plastics under Changing Loads"
W. N. Findley and Gautum Khosla, Brown University, Providence, R. I.

"Dynamic Mechanical Properties of Non-Polymeric Liquids
T. Litovitz and E. Carnevale
Naval Ordnance Laboratory, Washington, D. C.

"Diffusion Controlled Stress-Relaxation"
R. H. McMickle and E. T. Kubu
B. F. Goodrich Research Center, Brecksville, Ohio

"Contribution to the Study of Transport Phenomena in Gases at High Densities"
A. Michels, University of Maryland, College Park, Md. and A. S. Friedman, National Bureau of Standards, Washington, D. C.

"Preliminary Results in Measuring Dynamic Compressibilities"
W. Philippoff and J. Brodnyan, Franklin Institute, Phila., Pa.

"Theory of Non-Newtonian Flow. I. Solid Plastic System"
Taikyue Ree and Henry Eyring, University of Utah, Salt Lake City

"Theory of Non-Newtonian Flow. II. Solution System of High Polymers"
Taikyue Ree and Henry Eyring, University of Utah

"The Effect of Molecular Weight on the Creep Behavior of Cellulose Acetate Films"
D. L. Swanson, American Cyanamid Co., Stamford, Conn.
J. W. Williams, University of Wisconsin, Madison, Wis.

"Elastic Moduli as a Measure of Anistropy in Nylon and Dacron Fibers"
J. H. Wakelin, et al, Textile Research Institute, Princeton, N. J.

Papers which will appear elsewhere:

"Determination of Baking Values of Bread Doughs by Rheological Measurements"
C. W. Brabender and Stephen J. Loska, Jr., Pillsbury Mills, Inc., Minneapolis, Minn.

"Concentration Dependence of the Rheology Behavior of the Polyisobutylene-Decalin System"
T. W. De Witt, et al, Mellon Institute, Pittsburgh, Pa.

"Acoustics and the Liquid State"
R. B. Lindsay, Brown University, Providence, R. I.

"Rheology of Bituminous Pavements"
C. Mack, Imperial Oil, Ltd., Sarnia, Ontario

"A Cone and Plate Viscometer"
H. Markovitz, et al, Mellon Institute, Pittsburgh, Pa.

"Stress-Strain Relationships in Yarns when Strained at Impact Velocities Ranging from 10 to 100 meters per second"
Herbert F. Shiefer, National Bureau of Standards, Washington, D. C.

Papers which will not be published:

"Visco-Elastic Density Changes in Glass"
O. L. Anderson, Bell Telephone Laboratories, Murray Hill, N. J.

"The Statistical Mechanics of Viscosity"
Melville S. Green, National Bureau of Standards, Washington, D. C.

"Non-Linear Visco-Elastic Behavior in Shear of Rubber"
Herbert Leaderman, National Bureau of Standards, Washington, D. C.

"Some Remarks on the Theory of Non-Linear Visco-Elasticity"
R. S. Rivlin, Brown University, Providence, R. I.

F. R. Eirich, Editor
The Society of Rheology

Dynamic Creep of Plastics

Joseph Marin and W. J. Schuman
Pennsylvania State University

SUMMARY

A theory is presented in this paper for predicting creep in the case of fluctuating stress based upon an assumed creep-stress-time relation for simple static tension. The condition of dynamic stress considered is that of a completely reversed axial stress superimposed on a static axial tensile stress.

A second part of this paper presents both static and dynamic tension creep test results for a plastic designated as Plexiglas 1A. A comparsion of the theoretical dynamic creep-time relations based upon the static creep results agree approximately with the actual dynamic tension creep-time relations.

INTRODUCTION

Most available creep data for plastics is for static stress conditions. However, many applications of plastics include dynamic stress applications in which the stresses are not statically applied. The few investigations on dynamic creep that have been conducted have not included a theoretical interpretation of the test results. (1 to 5)*

The test results included herein were also reported in a paper presented at the 1954 U. S. National Congress on Applied Mechanics (6). However, the interpretation of these results is new.

PROPOSED THEORY FOR DYNAMIC CREEP

In order to develop a theoretical relation for dynamic tensile creep stresses, a relation for static tension creep strain will be selected, made up of the parts as indicated in Fig. 1, and based upon the following assumptions:

(1) The total strain of the material at a time t and constant temperature is composed of four component parts; the elastic strain, the initial plastic strain, the transient creep strain and the minimum creep-rate strain. These strains are shown in Fig. 1 for static stress. The subdivision of the strains is in keeping with considerations described by Orowan (7) and others, and can be represented by a model using springs and a dash pot.

(2) The elastic strain is proportional to the instantaneous stress or

$$\varepsilon_j = \sigma / E \qquad (1)$$

where ε_j = the elastic strain
σ = the instantaneous stress
E = Young's Modulus of Elasticity

(3) The initial plastic strain, for stresses greater than the proportional limit, takes place during the first loading cycle. This initial plastic strain will be assumed to be the following function of the maximum stress.

$$\varepsilon_h = k\,(\sigma\ max\ /\ E)^B \qquad (2)$$

where ε_h = the initial strain
$\sigma\ max$ = The maximum value of stress for stresses greater than the proportional limit
k and B = Experimental constants obtained from static stress-creep tests.

After the initial plastic strain, the material will behave elastically. Considerable test data for various materials indicates that eq. (2) is a good approximation.

(4) The transient creep strain is assumed related to the instantaneous stress by the condition that the transient creep rate is a function of the instantaneous stress and the transient creep strain, or

$$d\,\acute{\varepsilon}_h / dt = q\,(K\,\sigma^A - \acute{\varepsilon}_h) \qquad (3)$$

where $d\acute{\varepsilon}_h / dt$ = the transient creep rate
$\acute{\varepsilon}$ = the transient creep strain
q, K, A = experimental constants based on static stress-creep tests.

It is also assumed that the foregoing transient creep strain is fully recoverable. Equation (3) is used for expressing the transient creep since it gives a good representation for the early stage of creep.

(5) The minimum creep rate strain is assumed related to the instantaneous stress only, as given by the

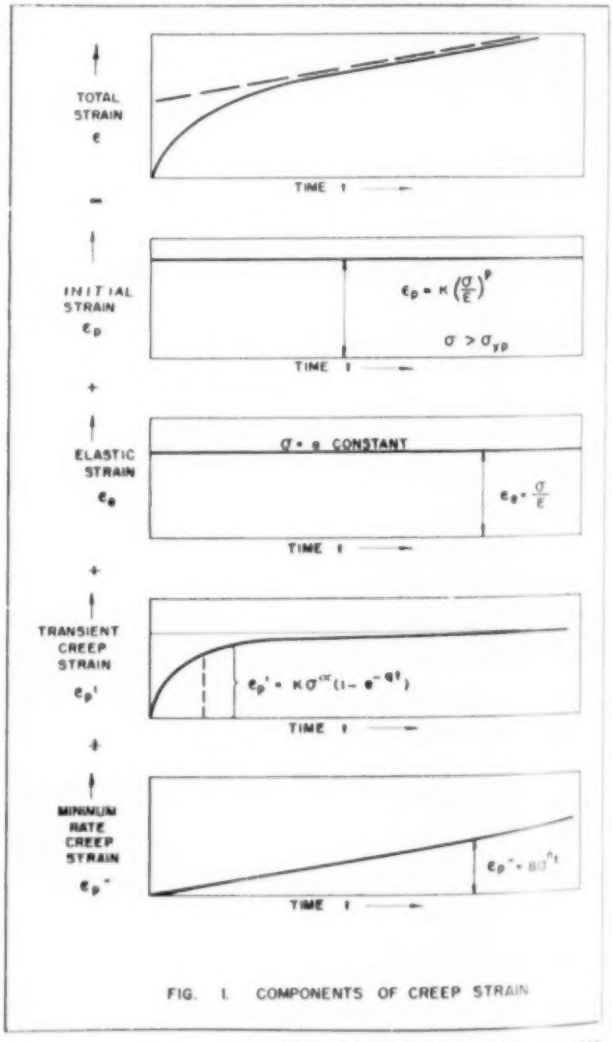


FIG. 1 COMPONENTS OF CREEP STRAIN

Numbers indicate references given in the Bibliography at the end of this paper.

following relation.

$$d\,\xi_b / dt = C\,\sigma^n \qquad (4)$$

where $d\xi_b/dt$ = the minimum constant creep rate

ξ_b = the minimum creep rate strain

C, w = experimental constants based on static tension creep data.

For many materials, eq. (4) has been found to be a good approximation. From equations (1) to (4), the total strain ε at a time **t** is

$$\varepsilon = \varepsilon_j + \varepsilon_b + \dot{\varepsilon}_b + \xi_b \qquad (5)$$

$$\text{or } \varepsilon = \sigma / E + k\,(\sigma\,max / E)^B + \int_o^t (d\dot{\varepsilon}_b / dt)\,dt + \int_o^t (d\xi_b / dt)\,dt$$

$$\text{or } \varepsilon = \sigma / E + k\,(\sigma\,max / E)^B + \int_o^t q\,(K\,\sigma^A - \dot{\varepsilon}_b)\,dt + \int_o^t C\,\sigma^n\,dt$$

It is desirable to represent the strain ε as a function of the stress σ. In eq. (5), the variable strain $\dot{\varepsilon}_b$ appears. The strain $\dot{\varepsilon}_b$ can be expressed in terms of the stress σ by using eq. (3). By solving eq. (3) and using the boundary condition that $\dot{\varepsilon}_b = 0$ when $t = 0$,

$$\dot{\varepsilon}_b = e^{-qt} \int_o^t e^{qt}\,qk\,\sigma^A\,dt \qquad (6)$$

Replacing $\dot{\varepsilon}_b$ in eq. (5) by the value from eq. (6),

$$\varepsilon = \sigma / E + k\,(\sigma\,max / E)^B + e^{-qt} \int_o^t e^{qt}\,q\,K\,\sigma^A\,dt + \int_o^t C\,\sigma^n\,dt \qquad (7)$$

Equation (7) represents the variation in strain with stress in terms of static creep constants. This equation will now be used to determine the creep strain-stress values for dynamic axial stress as represented in Fig. 2 and defined by the equation,

$$\sigma = \sigma\,m + \overline{\sigma} \sin \omega\,t \qquad (8)$$

where σ = the instantaneous stress

$\sigma\,m$ = the means or static preload stress

$\overline{\sigma}$ = the fluctuating superimposed stress

ω = the frequency of application of the fluctuating stress.

Placing values of σ from eq. (8) in eq. (7),

$$\varepsilon = 1 / E\,(\sigma\,m + \overline{\sigma} \sin \omega\,t) + k\,[\,(\sigma\,m + \overline{\sigma}) / E)^B + \int_o^t e^{qt}\,q\,K\,(\sigma\,m + \overline{\sigma} \sin \omega\,t)^A\,dt + \int_o^t C\,(\sigma\,m + \overline{\sigma} \sin \omega\,t)^n\,d$$

If A and **n** in eq. (9) are not integers — the case usually encountered — then closed solutions cannot be found. A solution can then be made by interpolating for the values of strain using the solutions for the nearest integer values of **A** and **n**. A more accurate method is to use a semi-graphical procedure for solving the two integrals in eq. (9). For this purpose, the first integral in eq. (9), representing the transient creep $\dot{\varepsilon}_b$, can be written,

$$\dot{\varepsilon}_b / \sigma^A\,m = e^{-qt} \int_o^t e^{qt}\,q\,K\,(1 + R \sin \omega\,t)^A\,dt \qquad (10)$$

where $R = \overline{\sigma} / \sigma\,m$

Graphical integration of eq. (10) can now be made by evaluating the integral of eq. (10) for various integer values of A, stress ratios **R**, and time **t**. In this way, sets of curves as shown in Fig. 3 can be obtained for values of A = 1, 2, 3 and 4 as indicated for each set of curves. Using Fig. 3, a set of graphs, as given in Fig. 4 can be drawn relating $\dot{\varepsilon}_b / \sigma^A\,m$ versus A for various values of **t** and **R**. Fig. 4 can then be used to find the value of $\dot{\varepsilon}_b / \sigma^A\,m$ for the value of A, time **t** and stress ratio **R**. The transient creep can now be obtained by multiplying the value selected from Fig. 4 by $\sigma\,m$.

The values of the second integral in eq. (9) can be obtained in a manner similar to that described for the first integral. With the foregoing integrals evaluated, the total strain ε can now be found using eq. (9). It should be noted that the foregoing theory assumes that a dynamic stress does not change the material properties.

COMPARISON OF THEORY WITH TESTS

Static and dynamic creep tests were conducted to check the validity of the foregoing theory. These test results have been previously reported (6) so that only a brief summary will be given here.

The material tested was a commercial grade of polymethylmethacrylate manufactured by Rohm and Haas Company and designated as Plexiglas 1A. The manufacturer lists the following properties for this material: tensile strength 6,000 to 10,000 psi, Modulus of Elasticity in tension 3 to 5 $\times$ 10^5 psi and percentage elongation 1 to 15%.

The tests were conducted in a creep laboratory with controlled temperature and humidity. The temperature was maintained at 73 $\pm$ 2 degrees F° and the humidity at 50 $\pm$ 2 percent relative humidity.

The specimens for both the static and dynamic tests had an overall length of 8 in. with a reduced test section of 3½ in. long and ½ in. $\times$ ¼ in. in cross-section. The constant static creep tests were conducted in the creep tension machine shown in Fig. 5. A photograph of the dynamic creep testing machine is shown in Fig. 6. A schematic drawing of the machine in Fig. 6 is shown in Fig. 7. Referring to Fig. 7, the specimen **S** is subjected to a mean stress by means of the lever **L** and the dead weight **W**. A constant speed, 1740 rpm, three phase induction motor drives the eccentric **E** which causes the lower end of the spring **SP** to move up and down in simple harmonic motion. This motion produces a fluctuating axial load on the specimen. A dynamometer **D** in series with the specimen is used to measure the fluctuating load. The dynamometer consists essentially of an aluminum bar with SR-4 strain gages bonded to it, calibrated to measure axial load. The creep strains in the specimens were measured by SR-4 gages attached to the specimens.

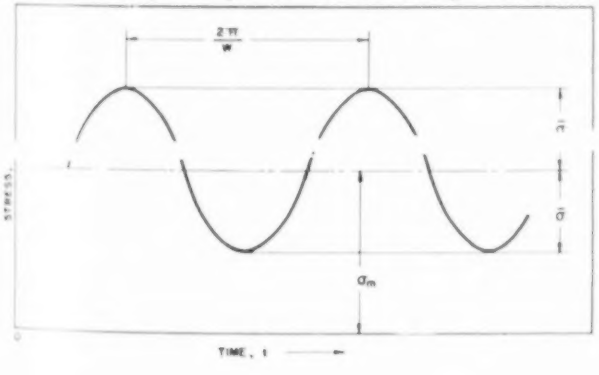


FIG. 2 VARIATION OF STRESS WITH TIME

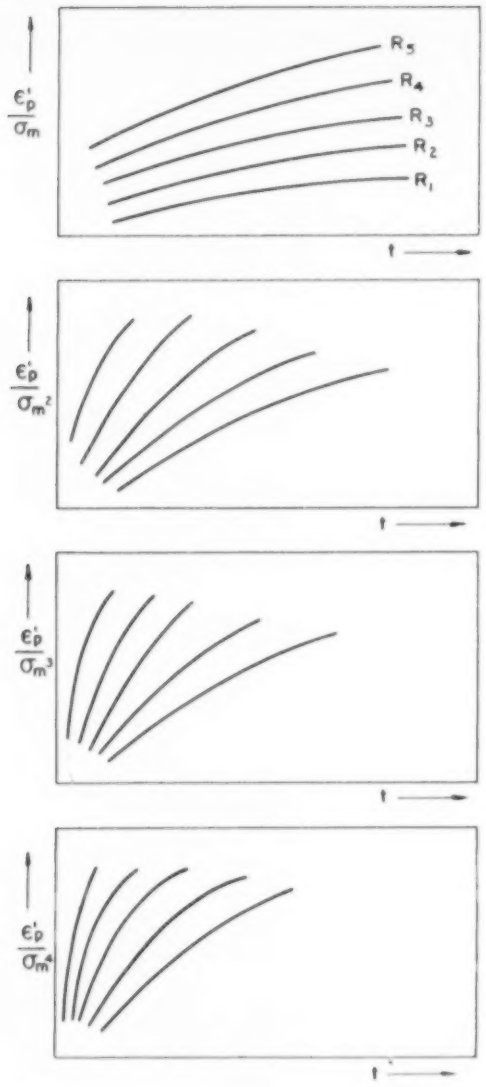


FIG. 3. $\frac{\epsilon_p'}{\sigma_m^{\alpha}}$ vs. t (FOR α = 1,2,3,4)

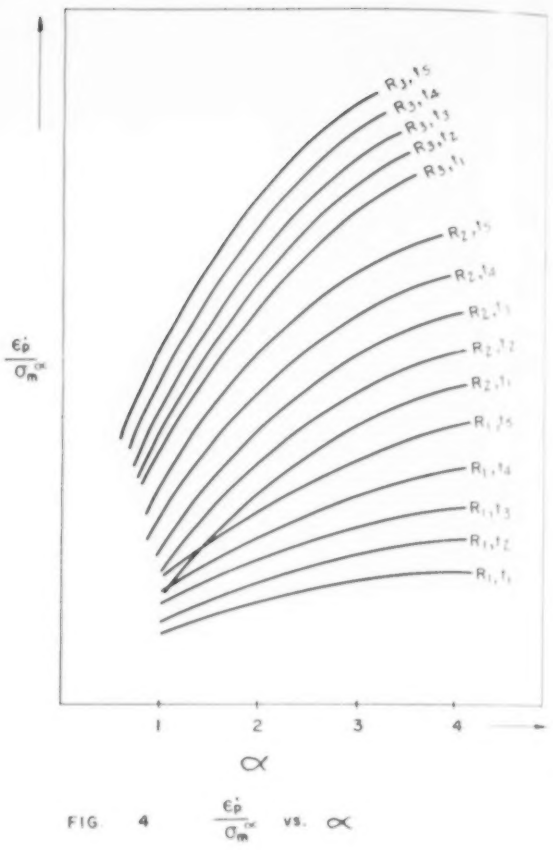
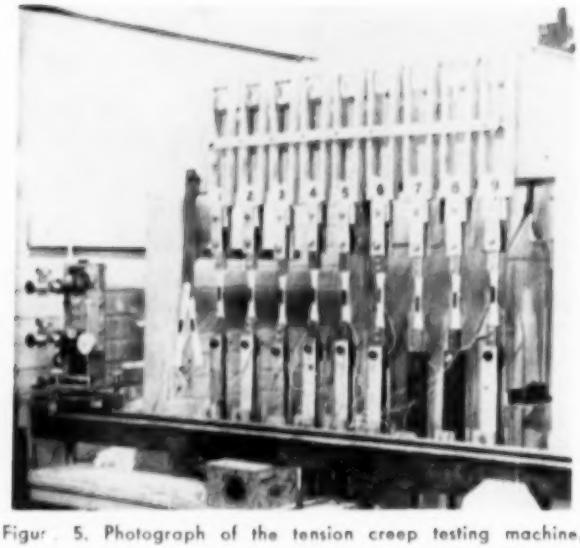


FIG. 4 $\frac{\epsilon_p'}{\sigma_m^{\alpha}}$ vs. α



Figur 5. Photograph of the tension creep testing machine.

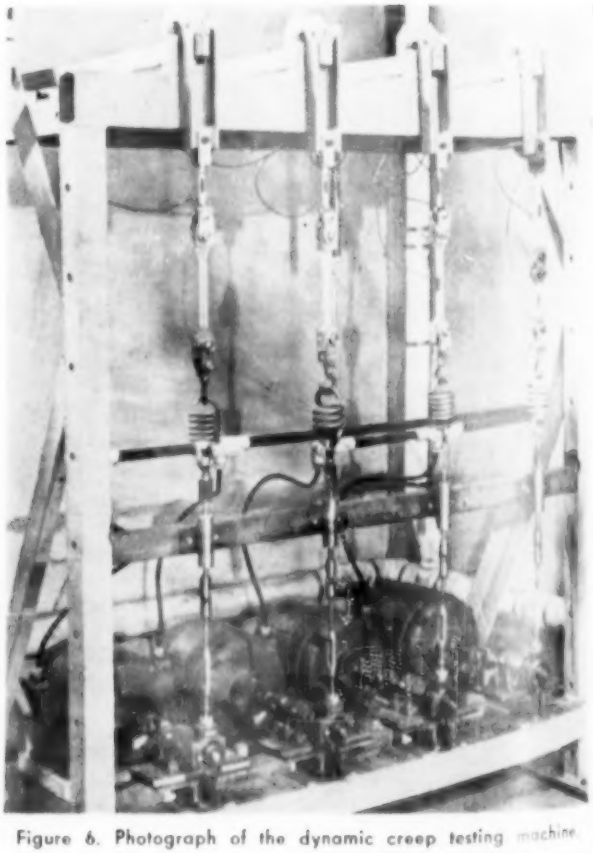


Figure 6. Photograph of the dynamic creep testing machine.

Fig. 6 shows a Hathaway Oscillograph with auxiliary equipment which was also used as a check for the measurement of both load and creep strain. These tests were conducted so that the mean loads remained constant as the specimens elongated.

The static creep-time relations, using the machine in Fig. 5, are given in Fig. 8. The theoretical creep-time relations, based on eq. (7), are also shown in Fig. 8.

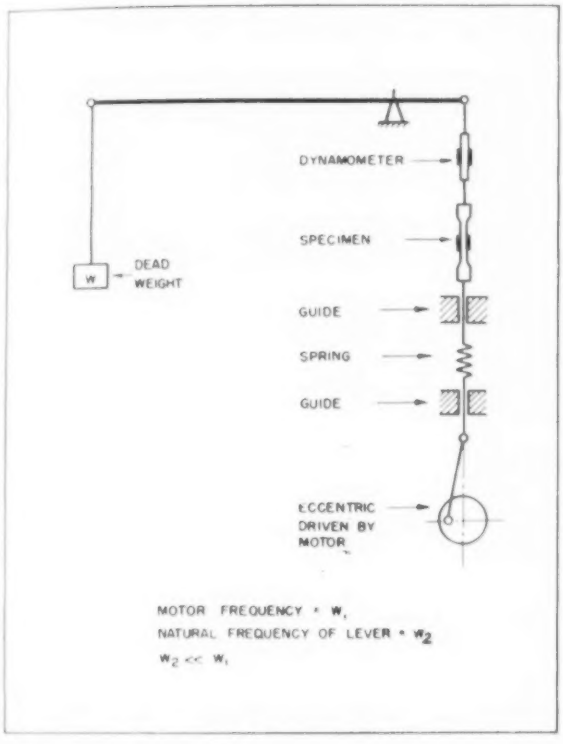


FIG 7 SCHEMATIC ILLUSTRATION OF ONE UNIT OF THE DYNAMIC CREEP TESTING MACHINE

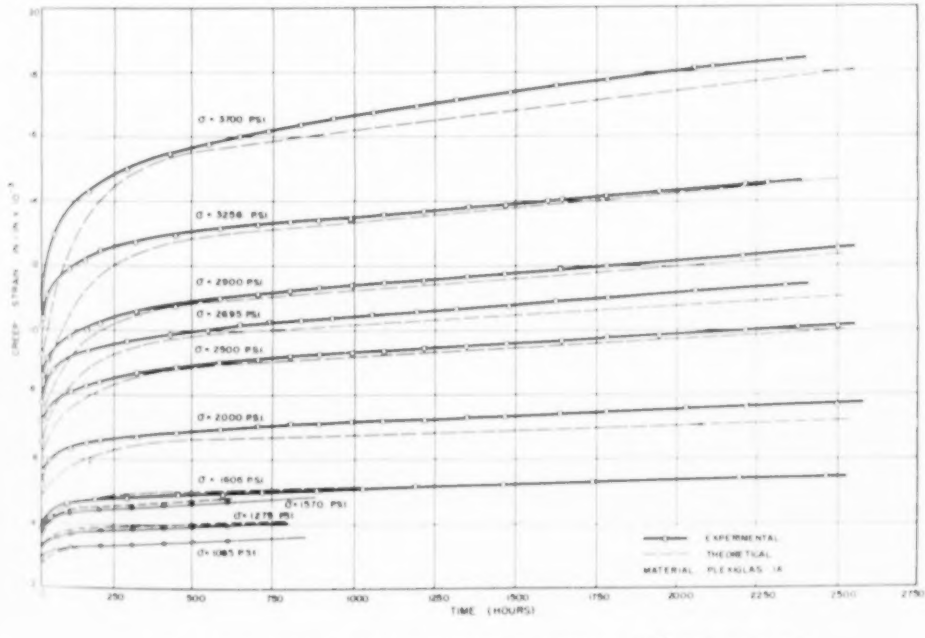


FIG 8 REPRESENTATION OF TENSION CREEP DATA BY EXPRESSION $\epsilon = \frac{\sigma}{E} + K\sigma^m(1 - e^{qt}) + B\sigma^n t$

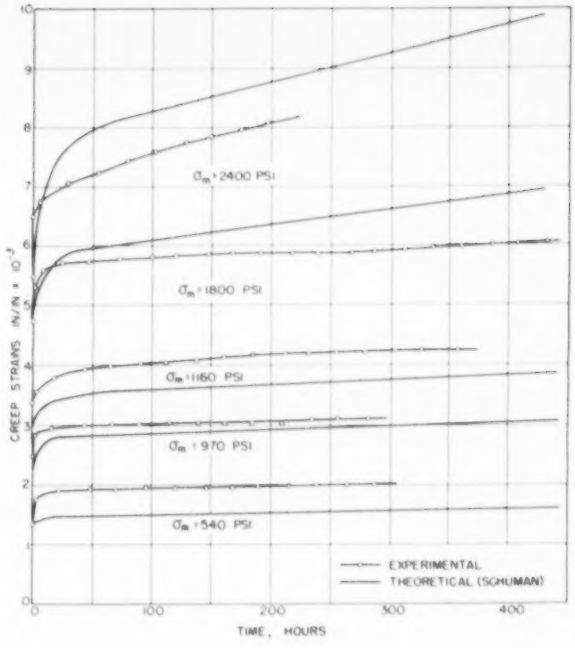


FIG 9 COMPARISON OF ACTUAL AND THEORETICAL DYNAMIC CREEP STRAINS

The experimental constants in eq. (7) were obtained by fitting the transient and constant creep rate strains using the method of least squares. The values of the constants obtained are: $E = 3.90 \times 10^5$, $K = 1.09 \times 10^{10}$, $B = 1.30 \times 10^{-13}$, $q = 0.14$, $A = 2.17$, and $n = 2.25$.

The dynamic creep test results are shown in Fig. 9. The five specimens tested were subjected to mean stresses as shown. The ratios of the variable to mean stress were 0.31, 0.17, 0.15, 0.09, 0.07 corresponding to mean stresses of 540, 970, 1160, 1800 and 2400 psi respectively.

Using the static tension creep constant based on Fig. 8 and eq. (7), the dynamic creep relations were obtained by the semigraphical method described in this paper. The resulting theoretical relations are shown in Fig. 9 by the dotted lines. A comparison of the theoretical and actual creep time relations in Fig. 9 shows approximate agreement between test results and theory.

CONCLUSIONS

A theory is developed in this paper for predicting dynamic creep-time relations based upon static creep values. Some preliminary test results are given to show that theory proposed can be used to predict creep strains under dynamic stress for the particular material, frequency and load range used.

(Please turn to page 54)

Effect of Fillers on the Dynamical Mechanical Properties of Polystyrene

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and Paul G. Richmond

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INTRODUCTION

The effects of reinforcing fillers such as carbon black in changing the mechanical properties of rubbers are well known and have been intensively studied. Much less is known, however, about the properties of rigid polymers containing fillers. Therefore, an investigation was carried out on certain mechanical properties of polystyrene containing fillers of different sizes, shapes, and mechanical behavior. The mechanical properties investigated were the dynamic shear modulus and damping. The measurements were made with a recording torsion pendulum at a frequency of about one cycle per second; this instrument has been described elsewhere (1). The damping is calculated as the logarithmic decrement from the curve of the damped oscillations. The shear modulus is calculated from the moment of inertia and frequency of the oscillating system and the dimensions of the test specimen.

EXPERIMENTAL RESULTS

The fillers were incorporated in the polystyrene by mixing on a mill roll. Test specimens (4" x ⅜" x 0.025") were pressed out between polished plates with a 0.025 inch spacer at 160°C and then cooled. Tests were carried out over the range 20°C to 105°C. Ground calcium carbonate, ground mica, powdered and fibrous asbestos, glass fibers, cotton cloth fibers and wood flour were used as fillers. The characteristics and concentrations of fillers used are listed in Table I. It was noted that the length of the glass and asbestos fibers was greatly reduced by the mill rolling.

TABLE I
Concentration and Description of Fillers Studied

Filler	Weight Per Cent	Density	Shear Mod. (Dynes/cm²)	Particle Size (cm)	Particle Shape
Mica	20 40 60	2.80	2.6 x 10^{11}	5 x 10^{-3} (diameter)	Flat Plates
Asbestos (Powder)	20 40 60	2.40	6.9 x 10^{11}	10^{-2}	Heterogeneous powder
Calcium Carbonate	8 14 20	2.70	2.3 x 10^{11}	10^{-3}	Rough Spheres
Wood Flour (Yellow pine)	5 10 15	0.50	0.95 x 10^{11}	10^{-2} length	Fibers
Glass Fibers	16	2.40	2.8 x 10^{11}	<0.1 10^{-3} (dia.)	Rods
Glass Fibers	19.9 22.3 18.3	2.40	2.8 x 10^{11}	1/16" orig. 1/8" orig. 1/4" orig.	Rods
Asbestos Fibers	40	2.40	6.9 x 10^{11}	0.1	Fibers with agglomerates
Cotton Cloth	20	1.48	2.8 x 10^{11}	5 x 10^{-2} length	Fibers

The experimental results are shown in Figures 1, 2, and 3 and in Table II. In general, the fillers increase the modulus over that of unfilled polystyrene, and they decrease the damping. The increase in modulus appears to be approximately proportional to the volume fraction of filler, the proportionality factor being dependent upon the type of filler. The powdered mica was outstanding in its reinforcing action while the wood flour had little effect on the modulus. The damping (logarithmic decrement) is a measure of the ratio of energy dissipated as heat to the recoverable energy stored in the specimen during a mechanical deformation. All the fillers except the glass fibers and cotton fibers decreased the damping at 30°C. Near 100°C polystyrene changes from a

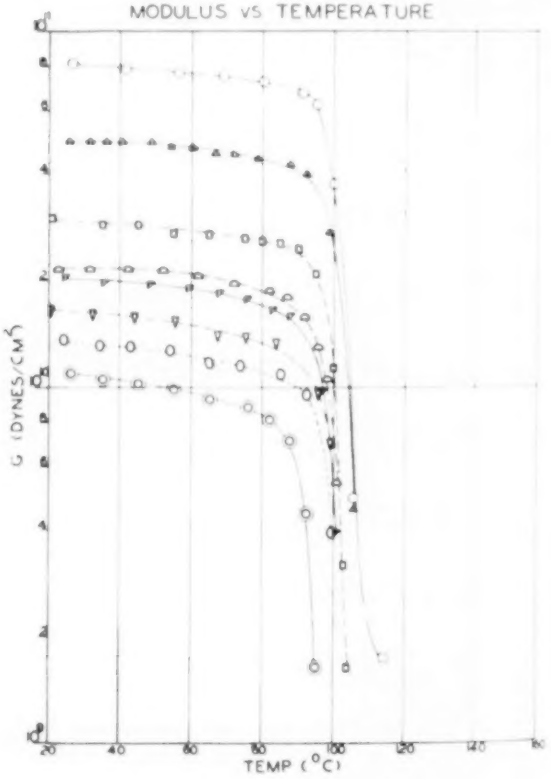

Fig. 1. Shear Modulus vs. temperature for filled polystyrene.

- Polystyrene control.
- 20% mica.
- 40% mica.
- 20% calcium carbonate.
- 40% asbestos.
- 60% mica.
- 20% asbestos.
- 60% asbestos.

TABLE II

Filler	Volume Per Cent	Modulus G (Dynes/cm²)	Logarithmic Decrements δ	Gδ	Adhesion Factor A
Mica	8.65	2.00×10^{10}	0.045	9.00×10^{8}	
	20.2	4.92	0.046	2.26×10^{9}	0.89
	36.3	8.00	0.033	2.65×10^{9}	
Asbestos Powder	9.98	1.63×10^{10}	0.048	7.80×10^{8}	
	22.8	2.13	0.036	7.70×10^{8}	0.086
	40.0	2.96	0.034	1.01×10^{9}	
Asbestos Fibers	22.8	1.95×10^{10}	0.054	1.32×10^{9}	0.074
Calcium Carbonate	3.30	1.15×10^{10}	0.058	6.67×10^{8}	
	6.03	1.27	0.044	5.60×10^{8}	0.20
	9.00	1.42	0.053	7.53×10^{8}	
Wood Flour	10.1	1.00×10^{10}	0.050	5.00×10^{8}	
	19.1	1.28	0.049	6.27×10^{8}	0.14 (?)
	27.3	1.15	0.053	6.08×10^{8}	
Glass Fibers	7.76	1.28×10^{10}	0.115	1.70×10^{9}	0.15
	18.3	1.64	0.080	1.31×10^{9}	0.15
	19.9	2.10	0.075	1.58×10^{9}	0.23
	22.3	1.96	0.078	1.53×10^{9}	0.17
Cotton Fibers	15.2	1.45×10^{10}	0.074	1.09×10^{9}	0.13
Unfilled Polystyrene		1.07×10^{10}	0.060	6.42×10^{8}	

rigid material to a rubbery one; in this temperature range the damping goes through a maximum. Rigid fillers shift this damping peak to slightly higher temperatures. The product of the modulus and the damping gives a quantity, Gδ, which is proportional to the heat generated in a material by a unit deformation. As shown in Table II, glass fibers, asbestos, cotton fibers, and mica cause more heat to be produced than in unfilled polystyrene while wood flour and calcium carbonate have little effect.

DISCUSSION

It is to be expected that the effect of a filler on the modulus should be proportional to its volume fraction, and as an approximation should obey the equation

$$G = G_1 \phi_1 + AG_2 \phi_2$$

or

$$G = G_1 \left[1 + (AG_2/G_1 - 1) \phi_2 \right]$$

since $\phi_1 + \phi_2 = 1$. In these equations G is the shear modulus and ϕ is the volume fraction; the subscripts 1 and 2 refer to polystyrene and filler, respectively. The quantity A is an "adhesion factor" which normally can vary between the limits zero and one. From elasticity theory it can be shown that for fillers with modulus considerably greater than polystyrene, A is somewhat less than 1.0 even if there is perfect adhesion between the polystyrene and the filler. A can be negative in cases where the filler has an apparent density in the plastic less than the density measured in bulk form; air entrapped with the filler can bring about such an effect.

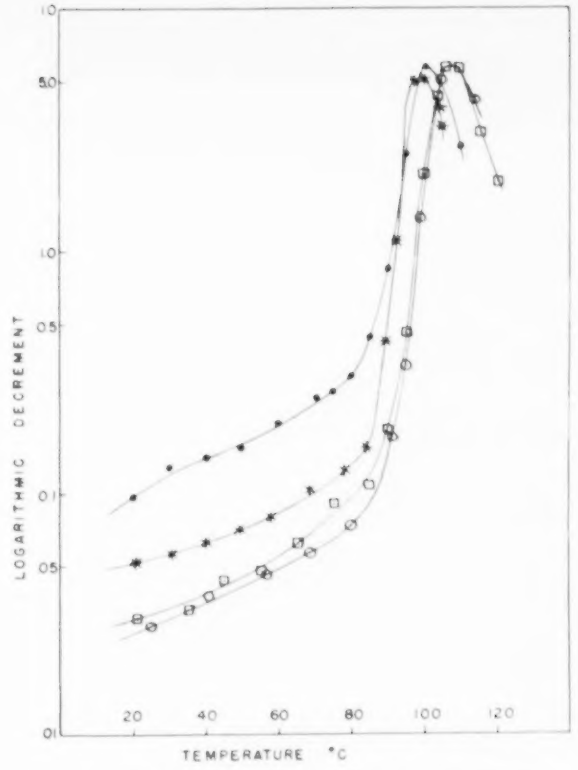

Fig. 2. Damping vs. temperature for filled polystyrenes.

✳ Polystyrene control. ⊡ 60% asbestos.
● 16% glass fibers. ⊙ 60% mica.

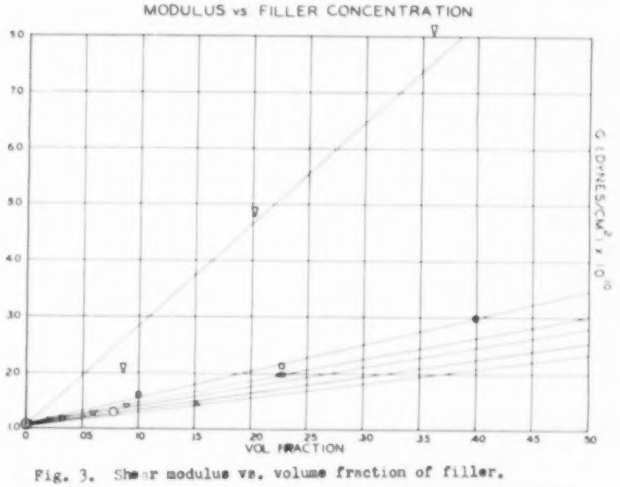

Fig. 3. Shear modulus vs. volume fraction of filler.

▽ mica, ▽ calcium carbonate,
□ asbestos, ⊙ glass fibers,
asbestos fibers, △ cloth fibers.

Figure 3 indicates that the shear modulus is approximately a linear function of the volume fraction up to at least 40 per cent. Knowing the shear modulus of the pure filler, it is then possible to calculate values of the "adhesion factor" A for the different fillers. The A values at 30°C are listed in Table II. These values may not be quite correct in all cases since the shear modulus of some of the fillers is not accurately known;

(Please turn to page 46)

Temperature, Velocity and Viscosity Distribution For a Temperature Sensitive Viscous Liquid

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ABSTRACT

A procedure is presented for computing the temperature, velocity, and viscosity distributions across a stream of a temperature sensitive viscous liquid in laminar flow when being sheared between two plane parallel plates. A general solution is given for a liquid the viscosity of which obeys an empirical logarithmic temperature dependence.

The use of the procedure for correcting experimental viscosity measurements is described using published data.

Application of the procedure to the design and operation of plastic processing apparatus is mentioned.

INTRODUCTION:

When a molten plastic material or other highly viscous substance is caused to flow mechanical energy is degraded into heat and the material temperature rises. If the rate of heat generation is sufficiently small the system can be maintained at essentially isothermal conditions by conducting the generated heat into the flow containing boundaries which act as a heat reservoir. However, often it is not possible to remove the generated heat until a substantial rise in temperature has been established and the system is not isothermal. This rise in temperature has been neglected by some investigators.

It is of interest to study the general case where temperature variations across the liquid stream are appreciable and to determine the temperature and velocity distributions. Such a study will facilitate a more adequate interpretation of flow curves and permit one to determine whether or not the temperature effects are sufficient to cause significant changes in the thermal, viscous, or chemical properties of the fluid.

There has been much discussion in the literature on this subject and in particular the work of Weltmann and Kuhns (9) is cited. They present a method for estimating the temperature distribution across the gap of a rotation viscometer and discuss the role of thixotropy and temperature in causing the decrease of viscosity when the material is subjected to shearing forces. When the thermal effects are ignored even a "Newtonian" liquid may be observed to have marked thixotropy and the flow curve may be interpreted as "non-Newtonian". Most plastic melts are considered to be non-Newtonian, the viscosity coefficient varying with shearing stress or rate of shear. Data obtained at low shear rates (less than about 4 sec.$^{-1}$) tend to confirm this conclusion. However, when one is concerned with the flow characteristics of these materials at rates of shear several orders of magnitude higher the data obtained at low shear rates are often unsatisfactory.

Goodeve and Whitfield (3) have presented arguments for conducting rotation viscometry tests at equilibrium torque, a procedure which is essential if one is to apply the results presented in this paper. Mill and Gates (5) studied the temperature effects in a rotation viscometer assuming that the rate of shear at the bob was not altered by the temperature rise. They obtained a corrected torque value by multiplying the torque at the base temperature by the ratio of the viscosity at the observed temperature divided by the viscosity at the base temperature. Merrill (4) described a coaxial cylinder viscometer for the study of liquids under high velocity gradients and discussed thermal effects. He installed a thermocouple in the liquid and measured the temperature rise showing it to be negligible. His conclusion is shown to be valid using the equations derived in this paper. Literature references on temperature effects in rotation viscometry have been summarized by Blok (1).

Teichmann (8) considered the case of viscous flow between plates and employed an average value for the fluid viscosity. Wilson and Mitchell (10) studied the self-induced temperature effects and their influence on the laminar flow of liquids. They considered the unsteady state case assuming the material was uniform across the flow path and varied with distance along the flow path. Brinkman (2) studied the heat effects in capillary flow and was concerned with the unsteady state case.

The procedure used in this paper was suggested by Prager and Rivlin (7) and is similar to a calculation that has been carried out by Nahme (6). Data of Weltmann and Kuhns (9) are used to provide a numerical comparison with their method. The system considered in this paper consists of two parallel plates which is a reasonable approximation for rotation viscometers when the ratio of the bob radius to the cup radius is near unity. The system also approximates the conventional smooth surfaced mixing torpedo at the end of an extrusion screw if the rotational velocity is large compared to the velocity along the torpedo. The method can be used to estimate the limiting temperature distribution across the extruder screw channel if the channel depth is less than about one tenth the width and the relative velocity of the screw surface with respect to the barrel surface is assumed to be along the channel axis.

SYSTEM AND LIMITING CONDITIONS:

Consider a system comprised of two rigid parallel plates whose length and width are large compared with the distance between them. A viscous liquid is contained between the plates and is caused to flow because of the steady motion of one of the plates along the reference axis in its plane. Each plate is maintained at a constant temperature, but the two plates are not necessarily at the same temperature. A steady state exists which is equivalent to stating that the point values of temperature, velocity, and viscosity depend only on the position across the stream, that is the Z coordinate. Laminar flow is presumed.

The particular viscosity temperature dependence law selected should provide adequate correlation over the range of temperatures existing in the system. For this reason the empirical exponential law given in eq. (1) is chosen. If the shear stress dependence of viscosity is known it can be incorporated into the equations since the shear stress is constant for a given dimensional set

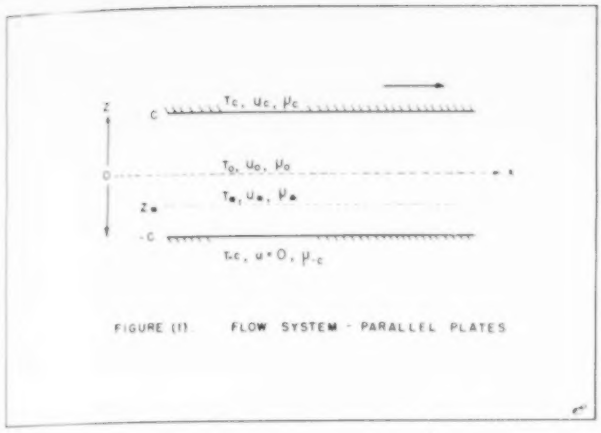


FIGURE (1) FLOW SYSTEM - PARALLEL PLATES

up of the plane parallel plate geometry and for a given mode of operation.

The system is pictured schematically in figure 1. The limiting conditions are:

(1)	(2)	(3)	(4)
$Z = C$	$Z = O$	$Z = Z^*$	$Z = -C$
$T = T(c)$	$T = T_o$	$T = T^*$	$T = T(-c)$
$u = u(c)$	$u = u_o$	$u = u^*$	$u = O$
$\mu = \mu(c)$	$\mu = \mu_o$	$\mu = \mu^*$	$\mu = \mu(-c)$

$$dT/dZ = O$$

When the two plates are at the same temperature, $T(-c) = T(c)$, and $Z^* = O$, because of the symmetry of the system. For convenience the value of $T(-c)$ is taken to be equal or greater than $T(c)$ which locates Z^* between position **-C** and **O**.

The basic equations are:

$$\mu = \mu' e^{-AT} \qquad (1)$$

where μ is viscosity, **T** is temperature, and μ' and **A** are constants;

$$du / dZ = S / \mu \qquad (2)$$

where du / dZ is the velocity gradient and **S** is the shearing stress;

$$S\, du / dZ = -J\, K\, d^2 T / dZ^2 \qquad (3)$$

where **K** is the thermal conductivity of the liquid and **J** is a factor for converting thermal units into mechanical units. Equation (3) is the differential equation stating the equality of heat flow from the system and mechanical energy dissipation within the system. At steady state all of the heat generated in the liquid by viscous shearing must be conducted out from the system.

TEMPERATURE DISTRIBUTION

Combining equations (1) and (2) and substituting in equation (3) one obtains,

$$d^2 T / dZ^2 = -2\lambda^2 e^{AT} / A \qquad (4)$$

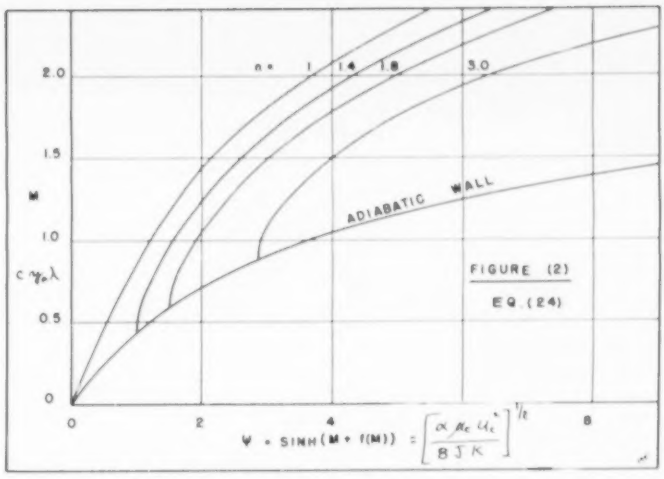
where $\lambda = (A\, S^2 / 2\, J\, K\, \mu')^{1/2}$

Equation (4) can be reduced to a first order equation and the variables separated if a new variable, $p = dT/dZ$, is defined. After integrating the result is,

$$(dT / dZ)^2 = -4\lambda^2 e^{AT} / A^2 + I \qquad (5)$$

The constant of integration **I** is evaluated using limiting condition (3) and equation (5) becomes,

$$(dT / dZ) = (\pm 2\lambda / A)\, (e^{AT^*} - e^{AT})^{1/2} \qquad (6)$$



(Alpha in the equations in the figure above is written as A in the text.)

The temperature gradient is positive when $-C \leq Z \leq Z^*$ and negative when $Z^* \leq Z \leq C$.

Equation (6) is solved by integration using the limits **-C** to **Z** and $T(-c)$ to **T** when (dT / dZ) is positive and Z^* to **Z** and T^* to $T(c)$ when (1) is negative. Using the notation,

$$y = e^{AT/2} \quad \text{and} \quad y^* = e^{AT^*/2}$$

the solution is,

$$y = y^* / \cosh [\cosh^{-1} y^* / y(-c) - (Z + C)\, y^* \lambda] \qquad (7)$$

when $dT / dZ \leq O$

$$y = y^* / \cosh [(Z - Z^*)\, y^* \lambda] \qquad (8)$$

when $dT / dZ \leq O$

An expression relating y^* and Z^* can be obtained from equation (7) when the values of $y = y^*$ and $Z = Z^*$ are substituted. The resulting expression is,

$$y^* = y(-c) \cosh [(Z^* + C)\, y^* \lambda] \qquad (9)$$

It is convenient to define three parameters for ease of numerical computation. These are,

$$n = y(-c) / y(c) \qquad n \leq 1 \qquad (10)$$

$$M = C\, y^* \lambda \qquad (11)$$

$$f(M) = -Z^*\, y^* \lambda \qquad (12)$$

The particular form of the function **f** will be established. Since $-Z^*$ is never greater than **C**, $f(M) \leq M$. Combining equations (9), (10), (11), and (12) and substituting into equations (7) and (8) one obtains,

$$y = y(-c) \cosh [M - f(M)] / \cosh [Z\, y^* \lambda + f(M)] \qquad (13)$$

when $dT / dZ \geq O$

$$y = y(c) \cosh [M + f(M)] / \cosh [Z\, y^* \lambda + f(M)] \qquad (14)$$

when $dT / dZ \leq O$

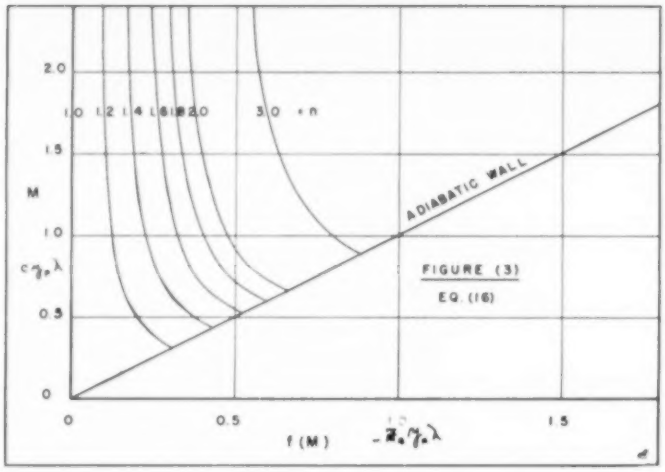
$$n = \cosh [M + f(M)] / \cosh [M - f(M)] \qquad (15)$$

From equation (15) it can be shown that

$$f(M) = \tanh^{-1} [(n - 1) \coth (M) / (n + 1)] \qquad (16)$$

which defines the function **f** in equation (12). Eq. (16) is plotted in figure 3 for several values of **n**.

Thus far it has been shown that the temperature distribution across the liquid stream can be represented by equations (13) and (14). If one knows the values for λ, y^*, **n**, and **M** the temperature at any position can be readily computed. One needs to know the values of **A**, **S**, **K**, μ' $T(-c)$, $T(c)$, and **C** to carry out this computation. The



difficulty is that usually the velocity of the boundary is known and not the shearing stress. Therefore, before proceeding with the numerical computations the equations for the velocity distribution will be established.

VELOCITY DISTRIBUTION:

Since $\mu = \mu' e^{-AT} = \mu' y^{-2}$ equation (2) can be written,

$$d u / d Z = S y^2 / \mu' \qquad (17)$$

When the expressions (13) and (14) are substituted into equation (17) and integrated for the limits **O** to **u** and **-C** to **Z** when **d T / d Z** $\geq$ **O** and for u* to u and **Z*** to **Z** when **d T / d Z** $\leq$ **O**, the results are,

$$u = \frac{S \cosh^2 [M - f(M)] \tanh [Z y^* \lambda + f(M)]}{\mu(-c) y^* \lambda} \qquad (18)$$

for d T / d Z $\geq$ O, and

$$u = u^* + \frac{S \cosh^2 [M + f(M)] \tanh [Z y^* \lambda + f(M)]}{\mu(c) y^* \lambda} \qquad (19)$$

for d T / d Z $\leq$ O

Substituting the limiting condition (3) into eq. (18) one obtains an expression for u*.

$$u^* = \frac{S \cosh^2 [M - f(M)] \tanh [M + f(M)]}{\mu(-c) y^* \lambda} \qquad (20)$$

Combining equations (19) and (20) one obtains,

$$U = \frac{S}{y^* \lambda} \left[\frac{\cosh^2 [M + f(M)]}{U_c} + \frac{\cosh^2 [M - f(M)]}{U_{-c}} \right] \tanh [Z y^* \lambda + f(M)] \qquad (21)$$

for d T / d Z $\leq$ O

Noting that $\mu(-c) = \mu(c) / n^2$ and from eq. (15) $n^2 = \cosh^2 [M + f(M)] / \cosh^2 [M - f(M)]$,

when the boundary conditions (4) are substituted into equation (21) one obtains,

$$u(c) = \frac{2 S \cosh^2 [M + f(M)] \tanh [M + f(M)]}{\mu(c) y^* \lambda} \qquad (22)$$

Substituting the value for y* given in eq. (9), the final expression for the velocity of the upper plate is obtained.

$$U_c = \left[\frac{8 J K}{A \mu_c} \right]^{\frac{1}{2}} \sinh \left[C y^* \lambda + \tanh^{-1} \left(\frac{n-1}{n+1} \coth C y^* \lambda \right) \right] \qquad (23)$$

It is interesting to note that when **Z*** = **-C**, the case of no heat flow into the lower boundary, that the value of **f (M)** is **M** and the last term of equation (23) reduces to sinh **(2M)**. The value of **n** is then,

$$n = \cosh [2 C y^* \lambda] \qquad (24)$$

When **Z*** = O, that is when **T(-c)** = **T(c)**, the value of **f (M)** is zero and **n** = 1. The last term of equation (23) reduces to the sinh **(M)**.

The value of C y* λ is completely defined by equation (23) when one is given values for **A, K,** $\mu(c)$, **T(-c)**, **T(c)**, and **u(c)**. The solution of equation (23) is readily obtained and is plotted in figure 2 for several values of n. The velocity group, ψ, is plotted as abscissa and the shear stress group, **M**, is plotted as ordinate.

$$\psi = [A \mu(c) U^2(c) / 8 J K]^{\frac{1}{2}} = \sinh [M + f(M)] \qquad (25)$$

COMPUTATION OF SPECIFIC PARAMETERS:

The maximum temperature, T*, is obtained by combining equations (23) and (14) when y = y*,

$$T^* = T_c + \frac{1}{A} \log_e \left[1 + \frac{\alpha \mu_c U_c^2}{8 J K} \right] \qquad (26)$$

This equation states that the maximum temperature rise, **(T*** $-$ **T(c))** is completely determined by the material properties **A,** $\mu(c)$, and **K,** and the velocity of the upper plate. This temperature rise is independent of the plate separation. The maximum temperature increases with increasing velocity, decreasing **A**, decreasing thermal conductivity, and is greater for more viscous materials. When the temperature of the lower boundary is increased the value of T* does not change until thermal energy is caused to flow into the material thru the boundary. The position of the maximum temperature moves towards **Z** = **-C** as **T(-c)** is increased but does not rise until after **Z*** = **-C** and only then if the boundary is at a higher temperature. For the system under discussion in this paper **T*** $\leq$ **T(-c)**.

To compute the temperature and velocity distributions the following procedure is suggested:

(1) The quantity Cy*λ is calculated from eq. (25) or figure (2).

(2) The parameter Z* y* λ is obtained from eq. (12) and (16) or figure (3).

(3) The parameter y* is obtained from eq. (9) or from eq. (26) and the definition, $y^* = e^{AT^*/2}$.

(4) The parameter λ is computed by combining (1), (2), (3) above

$$\lambda = C y^* \lambda / C y^* = M / C y^*$$

(5) The parameter Z* is obtained from (2), (3), (4) above.

$$Z^* = Z^* y^* \lambda / y^* \lambda = - C f(M) / M$$

(6) The velocity distribution is obtained from eq. (19) and (21).

(7) The temperature distribution is computed from eq. (13) and (14).

(8) The shearing stress can be computed from λ

$$S = [2 J K \mu' \lambda^2 / A]^{\frac{1}{2}}$$

If equations (25) and (9) are combined one obtains an interesting relationship between shearing stress and velocity described by equation (27) and plotted in figure 4.

$$\left[\frac{A C^2 S^2}{2 J K \mu_c}\right]^{\frac{1}{2}} = \frac{\sinh^{-1}\left[\frac{A u_c U_c^2}{8 J K}\right]}{\left[1+\left(\frac{A u_c U_c^2}{8 J K}\right)\right]^{\frac{1}{2}}}$$

For a given liquid and plate separation the shearing stress, S, increases to a maximum at $\psi = 1.5$ and then decreases.

NUMERICAL EXAMPLES:

Weltmann and Kuhns (9) computed the temperature distribution across the gap of a rotational viscometer for two materials, an Oronite polybutene oil and a silicone fluid. To compare their results with values calculated by using the equations developed in this paper one can compute the value of T* and Z* or if a more extensive comparison is desired the complete distributions can be compared.

For the Silicone fluid No. 5101C the data are:

K = 3.71×10^{-4} Cal. / sec. Cm. °C

$\mu(c)$ = 605 poise

T(c) = 30.0°, T(-c) = 37.65°C

A = 0.018° C $^{-1}$ obtained from figure 4 ref. (9)

u (c) = 47.1 cm. /sec.

C = 0.1 Cm.

J = 4.18×10^7 ergs / cal.

$$T^* = 30.0 + 1/0.018 \ln\left[1 + \frac{0.018 \times 605 \times 47.17^2}{8 \times 4.18 \times 10^7 \times 3.71 \times 10^{-4}}\right]$$

T* = 39.9°C and from Fig. I ref. (9) T* = 42.5°C.

$\psi = [0.018 \times 47.1^2 \times 605 / 8 \times 4.18 \times 10^7 \times 3.71 \times 10^{-4}]^{\frac{1}{2}} = 0.442$

n = e exp. [A (T (-c) − T (c)) / 2] = 1.071

M = 0.32

f (M) = .11

$y^* \lambda$ = M / C = 3.2

$Z^* = -$ f (M) / $y^* \lambda$ = -0.034 Cm.

The value of Z* estimated from figure 1 ref. (9) is −0.03 cm. which is a satisfactory check.

For the Oronite oil No. 32 the data are:

K = 3.06×10^{-4} cal /sec. cm. °C

$\mu(c)$ = 675 poise

T(c) = 30.0 °C, T(-c) = 37.65°C

A = 0.08 °C $^{-1}$ (obtained from fig. 4 ref. (9)

u (c) = 47.1 cm. /sec.

C = 0.1 Cm.

J = 4.18×10^7 ergs / cal.

$$T^* = 30.0 + 1/0.08 \ln\left[\frac{1 \times 0.08 \times 675 \times 47.1^2}{8 \times 4.18 \times 10^7 \times 3.06 \times 10^{-4}}\right]$$

T* = 39.7°C and from figure 2 ref. (9) T* = 41°C.

ψ = 1.081

n = 1.36

M = 0.67

f (M) = 0.27

$y^* \lambda$ = 6.7

Z* = −.04 Cm. and from fig. 2 ref (9) Z* = −.04 cm.

Merrill (4) measured the temperature rise for 10% Buna N in aniline being sheared in a rotation viscometer. He concluded that the temperature variation obtained was due to variations in the cooling fluid temperature. One can compute the temperature rise for his system as was done in the preceding examples. The data are:

K (aniline) = 0.039 in. # Force / sec. in. °C

$\mu(c)$ = 9.3 poise

T(c) = 21°C

A = (aniline) = 0.025°C^{-1} (estimated)

u(c) = 40.1 in / sec.

C = 0.003 in.

J = 1 (if poise are converted to # force sec. / in.^{2})

$$T^* - T(c) = \frac{1}{0.025} \ln\left[1 + \frac{0.025 \times 9.3 \times 1.453 \times 10^{-5} \times 40.1^2}{8 \times 1 \times 0.039}\right]$$

T* − T(c) = 0.7°C which is less than the observed variation and confirms Merrill's conclusions.

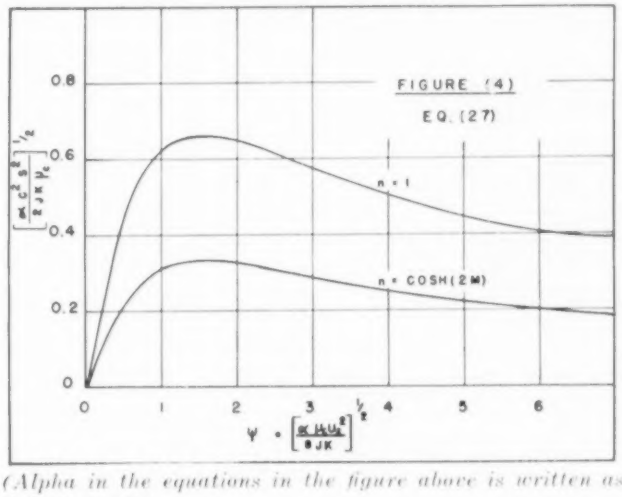
DISCUSSION:

If equations (13) and (14) are solved for the temperature, T, as a function of position, Z, and substitution is made into equation (1), an expression for the viscosity distribution across the liquid stream is obtained. The resulting equation is not convenient for numerical computation and as an alternative it is suggested that one compute the temperature distribution and then obtain the viscosity distribution by direct substitution for each position using eq. (1). The average temperature and average viscosity can be obtained from the plots of temperature and viscosity versus position. $\bar{T}$ does not necessarily correspond to $\bar{\mu}$ and one should compute A and μ' from point values of values of μ and T.

$$\bar{T} = \frac{\sum_c^c T_c \Delta Z}{2C} \quad (28) \qquad \bar{\mu} = \frac{\sum_c^c \mu_c \Delta Z}{2C} \quad (29)$$

An equation for the effective viscosity, which would be measured by a rotational viscometer operating under these conditions, is given in reference (9).

The rational design of plastic processing equipment such as the screw extruder has been limited greatly because of inadequate knowledge concerning the thermal and viscous properties of the materials being processed. Most attempts to correlate viscosity data at conditions



(Alpha in the equations in the figure above is written as A in the text.)

simulating those encountered in practical processes have failed to separate the temperature dependence from other sources of variation. This has restricted the range over which one could scale up the process and hope to obtain predictable results.

The manner in which operating conditions such as peripheral speed and boundary temperatures influence the temperature distribution can be estimated for limiting cases using the methods outlined in this paper. Meaningful viscosity data can be obtained using conventional rotation viscometers, and not restricting their range for isothermal operation. Instead one should measure the boundary temperatures and report a viscosity corrected for temperature increases.

The validity of the equations should be checked by means of experiment although it is quite likely that the computed values will be more accurate than those which one can measure. If the system is designed to have the bob boundary adiabatic, that is no heat flow into or out from the bob-plastic interface, then it should be possible to measure the bob surface temperature for comparison with the computed value.

ACKNOWLEDGMENT

The help and advice of Dr. F. D. Stockton who greatly aided in the successful completion of this work is acknowledged with pleasure. The author wishes to thank the Monsanto Chemical Company for the opportunity to pursue the problem and for permission to present this paper.

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The Analysis of Dynamic Tests of Visco-Elastic Materials*

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In this paper methods of analysis of dynamic tests of visco-elastic materials are examined. It is shown that it is important to develop the analysis on the basis of a general stress-strain relation, since the particular form of the relation for the material under test is not known in advance, and any arbitrary assumption about it may lead to contradictions. Such a general method of analysis is discussed for a simple longitudinal stress test, and for the vibrating reed test. References to the published literature indicate that such contradictions appear in currently accepted analyses, and their influence is detailed.

In seeking a visco-elastic model of springs and dashpots to represent the behaviour of the material, the method presented separates the analysis of the test results from the determination of the appropriate model.

INTRODUCTION

A common method of measuring the stress-strain characteristics of a visco-elastic material is to measure the motion of a dynamic system in which a specimen of the material is strained. Since the stress-strain characteristics are not known in advance, the dynamics of the system must be analyzed for a general visco-elastic material and the particular law governing the material under test must be deduced from the behaviour of the response. This approach has not always been adopted in the literature on the dynamic testing of polymers, and it has lead to errors in methods of interpreting test results. It is the purpose of this paper to clarify this question by discussing a convenient representation of the dynamic response of a system containing a general visco-elastic element, and to detail errors in the analysis of the vibrating reed test which have arisen in the literature because the theory was based on a special visco-elastic model.

We are concerned with simple configurations in which the stress and strain can each be represented by a single component, denoted by σ and ε respectively. These could be shear or normal components. Methods of testing are usually devised to utilize such simple forms of the stress and strain tensors. Linear behaviour is considered, so that a general visco-elastic relation takes the form

$$P(\sigma) = Q(\varepsilon) \qquad (1)$$

where **P** and **Q** are polynomials in the operator **D** = d/dt [1]*. For the well-known simple models, for example the Maxwell and Voigt materials, these polynomials are of zero or first powers in **D**, and the powers increase for more complicated materials. The dynamical analysis of a testing method must be carried out for general P and Q and the results represented in such a way that expressions for **P** and **Q** can be deduced which will reproduce the measured behaviour. A particularly convenient arrangement which has been commonly used is to employ forced

*This work was sponsored under a Department of Defense Contract NOrd 11496, with the Bureau of Ordnance, Navy Department.

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*Numbers in square brackets refer to the bibliography at the end of the report.

steady oscillatory loading of angular frequency ω so that the stress and strain can be represented by the real parts of

$$\bar{\sigma}\, e \exp.\, i\,\omega\, t, \quad \bar{\varepsilon}\, e \exp.\, i\,\omega\, t \qquad (2)$$

where $\bar{\sigma}$ and $\bar{\varepsilon}$ are complex magnitudes which therefore contain both amplitudes and initial phase angles. If the stress is of this form it follows from (1) that the strain is also, for in this case $\mathbf{D} = i\,\omega$, and (1) becomes an algebraic equation between $\bar{\sigma}$ and $\bar{\varepsilon}$. This leads to the usual notation for complex modulus:

$$\sigma / \varepsilon = \bar{\sigma} / \bar{\varepsilon} = E = E_1(\omega) + i E_2(\omega) = Q(i\omega) / P(i\omega) \qquad (3)$$

and complex compliance:

$$\varepsilon / \sigma = \bar{\varepsilon} / \bar{\sigma} = J = J_1(\omega) - i J_2(\omega) = P(i\omega) / Q(i\omega) \qquad (4)$$

This is not the only modulus which one could use, but it forms a convenient variable with which to measure the visco-elastic characteristics of a material. For a material obeying a relation of the type (1) it is known that analytically a knowledge of $E_1(\omega)$ or $E_2(\omega)$ in general prescribes the material properties completely (see for example the equivalent electrical problems discussed in [2], chap. XIV). For the analysis of experimental results which comprise values of E_1 and E_2 within the limits of experimental error over a finite frequency range, methods will be described elsewhere [9] for using both functions to determine a simple representation of the type (1) which reproduces the modulus variation approximately.

A more general complex modulus could be associated with response at complex frequency, for example:

$$\sigma = \bar{\sigma}\, e \exp(-\gamma + i\,\omega)t\,, \quad \varepsilon = \bar{\varepsilon}\, e \exp(-\gamma + i\,\omega)t \qquad (5)$$

and (1) would determine a complex modulus $Q(-\gamma + i\,\omega) / P(-\gamma + i\,\omega)$. Such a modulus would be more difficult to analyse than that corresponding to steady oscillation, since it is in general a function of two independent parameters. Such a modulus is obtained from measurements of the free oscillations of a system containing a visco-elastic element. Changes in the system to produce oscillation at varying complex frequency will in general modify both the real and complex components of the frequency which will introduce difficulties in interpreting the results. Steady state forced oscillation which provides response over a range of values of real frequency comprises a simpler representation of dynamic response, and this paper is mainly concerned with this type of test.

One of the commonest such methods of testing is to utilize a vibrating reed as shown schematically in Fig. 1. A reed of the material to be tested is clamped at one end to prevent rotation, the other end being free. The clamped end is given a steady oscillatory motion of amplitude δ_0 and frequency ω. The dynamic visco-elastic properties of the material are deduced from measurements of the oscillation of the free end over a range of frequency. Linearity can be checked by varying the amplitude δ_0; for a linear material δ will vary proportionally.

As illustrated in the next section of this paper for a simpler problem, the test results can be most easily analysed if the components of the displacement of **B** in phase and 90° out of phase with the steady oscillation of **A** are both measured, or equivalently, if the amplitude and phase of the oscillation of **A** are measured. In practice for the vibrating reed this proves to be a difficult task, and it is common to measure the amplitude of δ only. Thus for each frequency less information is obtained, but by taking measurements at resonance, for which the frequency is varied until maximum response is obtained, complete information about the complex modulus can be obtained. The resulting analysis is relatively simple if E_2, the imaginary part of the complex modulus is less than half the real part E_1. In order to obtain a series of resonance measurements over a range of frequency, the system must be modified, for example by shortening the reed, to produce a range of resonance frequencies. It is in the analysis of such a series of test results that care must be taken not to assume a visco-elastic model in order to interpret the results. Much of the difficulty would disappear if phase differences could also be measured, and this is achieved in some dynamic systems for measuring the physical properties of high polymers (see for example the Fitzgerald Apparatus [3]).

In the next section these questions are illustrated for a simple system of a rigid mass suspended from a rod of the material to be tested. The dimensions and frequencies used are considered to be such that the mass of the rod can be neglected so that simple uniform tensile or compressive stress occurs in the rod.

THE ANALYSIS OF A SIMPLE SYSTEM

The system to be considered is shown in Fig. 2. The support at the upper end permits vertical motion u_0 without rotation. The mass **M** at the lower end moves vertically with displacement u. If **l** is the length of the test specimen, and **A** its cross-sectional area, the longitudinal strain is $(u - u_0)/l$, and the equation of motion of the mass is:

$$M\ddot{u} = -A\sigma = -A(Q / P)[u - u_0) / l] \qquad (6)$$

in which the stress-strain relation takes the form (1), and formal division by the operator **P** has been carried out for convenience of expression.

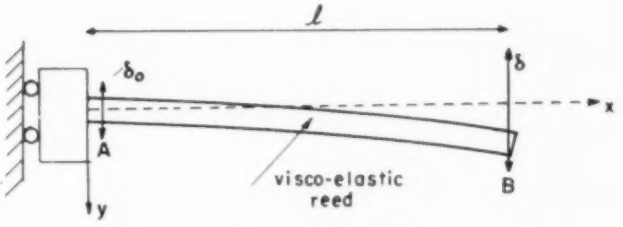

FIG. 1. THE VIBRATING REED TEST

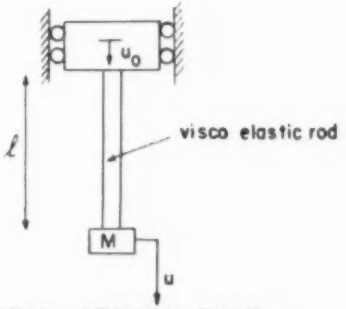

FIG. 2. A LONGITUDINAL STRESS TEST

For the case of free oscillations u_0 is zero, and the resulting strain can take the form

$$\varepsilon = \varepsilon_0 e \exp \gamma t = \varepsilon_0 e \exp (-\lambda + i\omega) t \qquad (7)$$

and substitution in (6) determines:

$$(Q / P) (\gamma) = -(M l / A) \gamma^2 \qquad (8)$$

where for this simple type of motion, the operators **Q (D), P (D)** become the complex functions **Q** (γ), **P** (γ). Thus if a single γ, which contains both the frequency component ω and the decay component $-\lambda$, is measured for a free oscillation, the complex modulus for complex frequency, **(Q / P)** (γ) = **E** (γ), is given directly by (8). However, as mentioned in the previous section, this is a rather complicated concept to utilize. For example if the rod were a Voigt material satisfying the relation

$$\sigma = (E_1 + \nu_1 D) \varepsilon \qquad (9)$$

(10)

$$\gamma = [-\nu_1 \pm i \sqrt{(4 E_1 M l / A) - \nu_1^2}] / (2 M l / A)$$

$$E(\gamma) = \frac{P}{Q} = \left(E_1 - \frac{E_1^2 A}{2 M \ell}\right) + i \nu_1 \sqrt{\frac{A}{M \ell}\left(E_1 - \frac{E_1^2 A}{4 M \ell}\right)} \qquad (11)$$

In the case of forced steady oscillation with frequency ω we have simply

$$E(\omega) = E_1 + i \omega \nu_1$$

The form of (11) indicates that even for a very simple model the utilization of complex frequency, which is implicit in damped free vibration, expresses visco-elastic properties in a very complicated manner, compared with steady forced oscillation. If the damping coefficient is sufficiently small compared with the real part of the frequency, ω, this complexity can be avoided by using an approximate analysis (see [4], p. 701).

For steady forced oscillation $u_0 = \bar{u}_0 e \exp i \omega t$, $u = u e \exp i \omega t$, where $\bar{u}_0$ and u are complex quantities. (6) reduces to an expression for the response ratio as a function of the frequency ω

$$\frac{\bar{u}}{\bar{u}_0} = c = \frac{1}{1 - \frac{P(i\omega)}{Q(i\omega)} \frac{M \ell \omega^2}{A}} = \frac{1}{1 - \psi} \qquad (12)$$

where ψ is defined as

$$\psi(\omega) = \psi_1 + i \psi_2 = P(i\omega) M l \omega^2 / Q(i\omega) A \qquad (13)$$

Alternatively we can express the modulus or compliance (Eqn. (3) and (4)) as a function of the response ratio c:

$$E(\omega) = E_1 + i E_2 = M l \omega^2 c / A (c - 1) \qquad (14)$$

$$J(\omega) = J_1 - i J_2 = A (1 - 1/c) / M l \omega^2 \qquad (15)$$

Thus if the complex response ratio c is measured for a range of forcing frequencies ω, (14) and (15) give the complex modulus and compliance directly.

If only the ratio of the amplitudes $|c|$ can be measured it is not possible to determine the complex modulus directly. However, if a series of measurements of this ratio at resonance is made for a range of resonance frequencies obtained by varying the mass **M**, the variation of complex modulus with frequency can be determined. To illustrate the significance of a resonance measurement, (12) gives the amplitude ratio in the form

$$|c| = \frac{1}{|1 - \psi|} = \frac{1}{\sqrt{(1 - \psi_1)^2 + \psi_2^2}} \qquad (16)$$

Thus we can plot the amplitude ratio on the ψ plane, and as shown in Fig. 3 the contour lines are concentric circles with center (1, 0). As the radius decreases the amplitude ratio increases, and it approaches infinity as the center is approached for this point corresponds to resonance of a purely elastic system. The expression for the response in terms of the function ψ is not dependent on the particular forms of **P** and **Q** corresponding to a given material. The response relations (12) and (16) can therefore be used to analyse test results before the operators **P** and **Q** have been determined.

A resonance test consists of varying the frequency around that which produces maximum response, and in view of (16) this occurs when ψ is closest to unity. This varying frequency is associated with fixed values of **M**, **l** and **A**, so that in view of (13) response values will be measured on a trajectory in the ψ plane, such as ABC in Fig. 3. Since P / Q = 1 / E, E_2 positive demands that ψ_2 be negative. The trajectory ABC will be given by (13) with variable ω, and will thus depend on the form of the operators **P** and **Q**. For different resonance frequencies obtained by changing **M**, the corresponding trajectory will change according to an expansion by a constant factor about the origin. This will of course bring a different range of ω, and correspondingly of **P** $(i\omega)$, **Q** $(i\omega)$ into the region $\psi \simeq 1$, which influences the measurement at resonance.

For the trajectory ABC, the maximum response occurs at **B** where the curve is tangent to a contour. In the case $E_2 << E_1$, so that $\psi_2 << \psi_1$, we can use measured values of resonance response, combined with the condition for the maximum at resonance, to deduce $E_1(\omega)$ and $E_2(\omega)$. For with $\psi_2 << \psi_1$, the trajectory ABC will be inclined at a small angle to the ψ_1 axis in view of the definition (13). The point of tangency with the contours, **B**, therefore lies close to the vertical diameter $\psi_1 = 1$. Writing

$$\psi = 1 - \varepsilon - i b \qquad (17)$$

in the region of resonance this requirement demands $1 >> b >> |\varepsilon|$. In terms of ε and **b** the amplitude ratio of response to forcing displacement is given by:

$$|c|^2 = 1 / (\varepsilon^2 + b^2) \qquad (18)$$

which follows directly from (16) and (17).

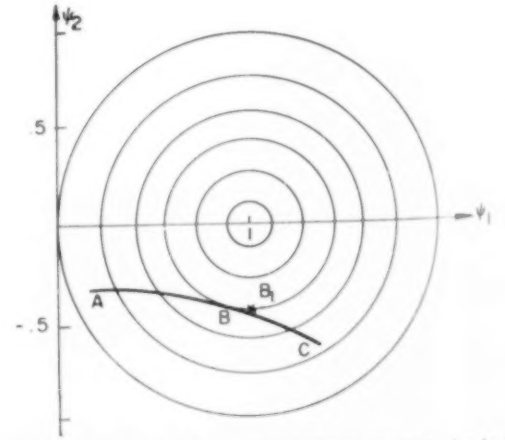

FIG. 3. CONTOURS OF THE RESPONSE RATIO $|u/u_0|$

The measured value of $|c_j|$ at the resonance frequency ω_j thus provides a first approximation for ψ_j given by $\varepsilon = 0$, $b = 1 / |c_j|$ corresponding to the point B in Fig. 3. That is:

$$\psi_j = 1 - (i / |c_j|) \tag{19}$$

(15) determines the corresponding values of the compliance to be:

$$J[\psi(\omega_j)] = i J_1[\omega_j] = (A / M l \omega_j^2)[1 - (i / |c_j|)] \tag{20}$$

Such a first approximation can be obtained for each value of ω_j for resonance with different values of the mass M. These results provide a first approximation to $J_1(\omega)$, $J_2(\omega)$ from which an approximation to the trajectory ABC for each resonance point can be obtained, leading to a more accurate value for the compliance at the resonance point **B.**

Resonance occurs when

$$d|c|/d\omega = 0 \tag{21}$$

for constant **M, A** and **l.** Using (18) this reduces to

$$\varepsilon \, d\varepsilon / d\omega + b \, db / d\omega = 0 \tag{22}$$

and the derivatives can be expressed in terms of the derivatives of the compliance using equations (12, 13, 14, 15 and 17)

$$\varepsilon = 1 - J_1(M l \omega^2 / A) \quad ; \quad b = J_2(M l \omega^2 / A) \tag{23}$$

$$d\varepsilon / d\omega = -(M l / A)(\omega^2 \, dJ_1 / d\omega + 2\omega J_1);$$
$$db / d\omega = (M l / A)(\omega^2 \, dJ_2 / d\omega + 2\omega J_2) \tag{24}$$

Using the values of J^1 and $dJ^1/d\omega$ given by the first approximation, in eqs. (24), (22) and (18) together with the measured value of $|c|$ a second approximation for ε and **b** can be determined and so of $J_1(\omega_j)$ and $J_2(\omega_j)$. The resulting changes in **J** will usually be sufficiently small for this approximation to correspond to the correct resonance point **B** in Fig. 3. If not the iteration can be repeated. Thus the real and imaginary part of the compliance can be determined from measurements of the amplitude ratio at resonance only, without a measure of the phase difference. The gain in experimental simplicity is, however, balanced against considerably greater analytical complexity. In practice, quite apart from the considerations discussed here, resonance methods are convenient because the response reaches its maximum amplitude and is therefore amenable to more accurate measurement.

THE VIBRATING REED TEST

The interpretation of the vibrating reed test closely parallels the analysis given in the previous section for a simple dynamic longitudinal stress test. Since the dimensions of the cross-section of the reed are usually small compared with its length, a development of the usual elastic beam vibration theory can be used with Young's modulus **E** replaced by the operator **Q / P.** The equation for lateral displacement y in terms of the mass per unit length **m**, and the second moment of the section **I**, becomes (see for example [5]

$$(Q / P) I \partial^4 y / \partial x^4 + m \partial^2 y / \partial t^2 = 0 \tag{25}$$

The use of the operator **Q / P** in (25) is formal, and to obtain a partial differential equation, we must multiply by **P** giving

$$I Q \partial^4 y / \partial x^4 + m P \partial^2 y / \partial t^2 = 0 \tag{26}$$

The order of this equation depends on the operators P and Q. It can be developed directly from the equation of motion and the assumption that plane sections of the reed remain plane, just as the elastic case is developed.

In the vibrating reed test we consider steady forced oscillation with frequency ω. Thus the displacement y is of the form:

$$y = \bar{y} \, e \exp i \omega t \tag{27}$$

so that the operator $D = \partial / \partial t$ can be replaced by multiplication by $i\omega$. (25) and (26) become

$$d^4 y / dx^4 - [m \omega^2 P(i\omega) / I Q(i\omega)] y = 0 \tag{28}$$

This is similar to the equation for forced oscillation of an elastic rod, with the difference that the coefficient of $\bar{y}$ is in this case complex, and dependent on the complex modulus $E(\omega) = Q(i\omega) / P(i\omega)$. (28) can be solved directly to satisfy the boundary conditions in the simplest case of an unloaded reed:

$$x = 0 \;,\; \bar{y} = \delta_0 \;,\; \partial \bar{y} / \partial x = 0 \tag{29}$$

$$x = l \;,\; \partial^2 y / \partial x^2 = \partial^3 \bar{y} / \partial x^3 = 0$$

Solution of the differential equation determines the motion of the free end to be given by

$$y(l) = \bar{y}(l) e^{i\omega t} = \delta e^{i\omega t} = \delta_0 \frac{\cosh\Phi + \cos\Phi}{1 + \cosh\Phi \cos\Phi} e^{i\omega t} \tag{30}$$

where Φ is defined as:

$$\Phi = \left[\frac{m\omega^2 P(i\omega)}{I Q(i\omega)}\right]^{\frac{1}{4}} l = \Phi_1 + i\Phi_2 \tag{31}$$

δ / δ_0, the amplitude ratio between the free and clamped ends of the reed shown in Fig. 1, is a complex number indicating that the motions of the two ends of the reed are in general not in phase. The modulus $|\delta / \delta_0|$ only is normally measured in a vibrating reed test.

The situation represented by (30) and (31) for the vibrating reed parallels closely (12) and (13) of the last section. The response amplitude ratio for steady forced oscillation is given by (30), and this relation is independent of the material properties expressed by the operators **P** and **Q.** For varying frequency with a given reed, which represents the conditions occurring in a resonance test, (31) gives the trajectory in the Φ plane which determines the variation of the response. The response function (30) is much more complicated than (12), and contains an infinite series of resonance peaks corresponding to the modes of vibration of an elastic reed. In practice measurements are usually taken of excitation at frequencies in the region of the natural frequency of the fundamental mode of the reed. This provides the lowest resonance frequency. Contours of the modulus of the response ratio in this region are shown in Fig. 4. Resonance for the fundamental mode of the elastic system is represented by the point **D. E** represents a saddle point, and beyond it the response ratio begins to rise again to the second resonance peak. Because of the fourth root in (31), points within the angle CAE and on its radii represent all posi-

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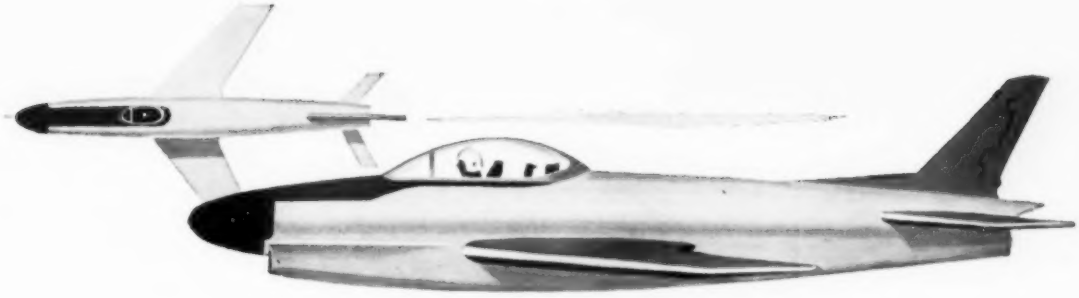

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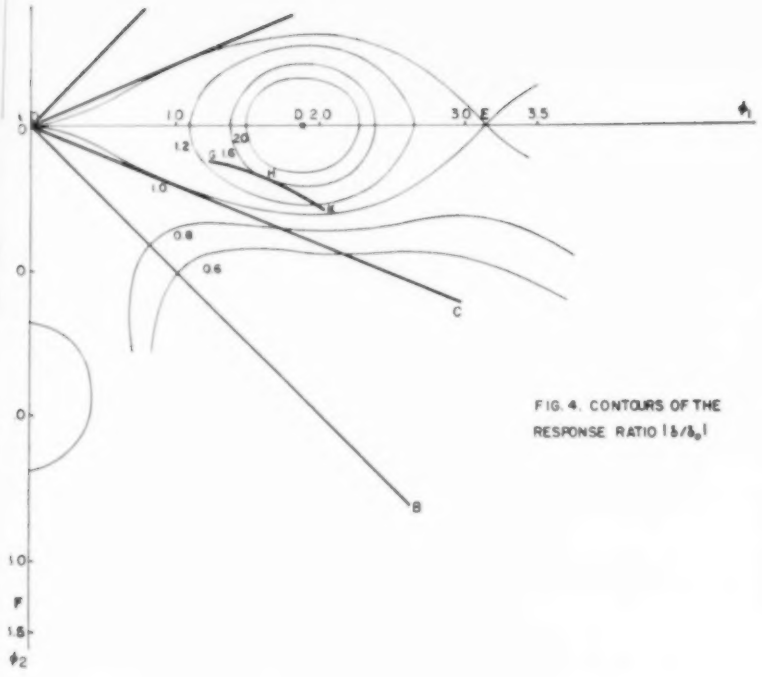

FIG. 4. CONTOURS OF THE RESPONSE RATIO $|\delta/\delta_0|$

tive values of E_1 and E_2 from zero to infinity. Thus only this region is needed for the interpretation of the vibrating reed test. The angle CAF includes negative values of the moduli. The same function is repeated in the other quadrants corresponding to the three other fourth roots of the complex number in (31). For this reason the variation of the amplitude ratio along AF is the same as along AE. The contours are symmetrical about AB.

A vibrating reed resonance test can be analyzed on the basis of (30) and (31) just as the simpler longitudinal test was treated in the previous section. The analysis is considerably more involved, particularly for a loaded reed, and will be given elsewhere [10]. As the frequency is varied around the resonance frequency, a trajectory is traversed in the ϕ plane, such as GHK in Fig. 4. Maximum response occurs at H where the trajectory is tangent to a contour line. To obtain the real and imaginary part of the modulus from amplitude measurements only, a series of resonance measurements must be made, for example by varying the length l of the reed. This varies the fundamental frequency, and determines a series of different trajectories in the region of the resonance peak D. The method of analysis mentioned above determines these resonance points H, and the corresponding complex modulus or compliance at each resonance frequency.

In the analysis of the vibrating reed test it has been common (see for example [6] and [7]) to use the basic partial differential equation for lateral vibration of a reed of Voigt material:

$$u\,\partial^2 y / \partial^2 t + E_1 I\,\partial^4 y / \partial x^4 + \eta I\,\partial^5 y / \partial x^4 \partial t = 0 \qquad (32)$$

in place of the more general partial differential equation (26). To make this restriction at the beginning strictly speaking prescribes the type of variation of complex modulus with frequency that can be treated, but this difficulty is avoided by permitting E_1 and η in (32) to vary with frequency. This of course is a meaningless modification of (32) as a partial differential equation, and only has significance when consideration is restricted to steady sinusoidal solutions with respect to time. This difficulty can be overcome as in the present analysis by using (20) with P and Q unspecified. While the above can be considered as purely a formal criticism, we shall see below that it has lead to errors in the analysis of resonance tests.

Nolle's [8] approach, which represents the reed by an equivalent single degree of freedom electric circuit, suffers from the equivalent restriction as detailed below. However, in analysing the resonance test, Nolle points out this short-coming (p. 773 of [8]) but accepts it in order to simplify the analysis. From a practical standpoint the error involved is not significant if E_2 is small compared with E_1. Hillier [6] also limits himself to an approximate analysis. In contrast, Horio and Onogi [7] claim an exact analysis to clear up the difficulties mentioned by Nolle. However, the limitations of their analysis do not permit the higher order terms to be determined correctly.

Horio and Onogi's analysis is based on expansion of the expression for the modulus of the amplitude ratio (which would be obtained directly from eqn. (30) above) for $E_2 << E_1$. ϕ, defined in (31), is written as $a + i\,b$, and resonance is defined by the condition

$$(d / d\,a)\,(\delta^2 / \delta_0^2) = 0 \qquad (33)$$

In Fig. 4 this is equivalent to prescribing the trajectory GHK to be parallel to the ϕ_1 axis, which effectively prescribes the form of P and Q for the frequency variation in the resonance test. A resonance analysis based on this premise can only give the first approximation similar to that discussed in the previous section (this corresponds to the point B_1 in Fig. 3). For a more thorough analysis, the influence of the particular forms of P and Q corresponding to the material under test must be included, as in the analysis detailed in the previous section. The forms of P and Q are to be determined from the test results, so that an iteration procedure of the type used is necessary to obtain their behaviour and include its effect in the analysis.

We thus see that the main difficulty in interpreting a resonance test is that the conditions under varying frequency correspond to a certain trajectory such as GHK in Fig. 4. If an initial assumption is made such as that by Horio and Onogi that GHK is horizontal, in general the deduced modulus variation will not be consistent with this assumption. Nolle's first order analysis which is based on an equivalent electric circuit with fixed components in effect assumes that E_1 and E_2 / ω are constant. For a particular resonance measurement, this will prescribe a certain trajectory in the ϕ plane, but other than by coincidence this will not agree with that based on the deduced variation of E_1 and E_2 with frequency. Strict application of the vibration equation (32) also prescribes this trajectory in advance. For an accurate analysis, freedom must be retained initially, and the trajectory determined in conformity with the deduced material properties.

In the above discussion we were concerned with deduction from the measured amplitude response ratio at resonance. If instead the width of the resonance peak is measured for which the amplitude is $1 / \sqrt{2}$ of the maximum, the need to analyze the results by considering the correct trajectory will be more important, since measurements over an extended length of the trajectory are involved. A trial and error method would be needed in conjunction with a contour diagram such as Fig. 4.

DISCUSSION

We have seen above that it is possible to analyze a dynamic test of a visco-elastic material on the basis of the general stress-strain relation (1), and to interpret the results in the form of the determination of $\mathbf{Q}\ (i\ \omega)\ /\ \mathbf{P}\ (i\ \omega) = \mathbf{E}\ (\omega)$ over the range of ω tested. This function represents the mechanical properties of the material in this frequency range. The problem of determining a corresponding visco-elastic model composed of dash-pots and springs then consists of obtaining a model which reproduces $\mathbf{E}\ (\omega) = \mathbf{E}_1\ (\omega) + i\ \mathbf{E}_2\ (\omega)$ for the modulus as a function of frequency. In the analysis of the test itself, the existence or form of the model does not appear. As detailed above, it is important that the interpretation of test results should be carried out consistently with this concept in mind, in order to avoid placing unwarranted constraints on the analysis.

The visco-elastic relation (1) has been used in this paper in place of the more general definition of a hereditary material with a continuous, rather than discrete, spectrum of relaxation times. The analysis in fact includes the more general concept since $\mathbf{Q}\ (i\ \omega)\ /\ \mathbf{P}\ (i\ \omega)$ is sought without restriction as to its form. As deduced from (1) it must be a rational function, but is not so restricted in the general hereditary case. In the practical analysis of experimental results this distinction does not arise since approximation within experimental error is all that can be sought. If behaviour over a limited frequency range only is required, as for example in the analysis of the motion of a system within a limited period of time, comparatively simple forms for $\mathbf{P}$ and $\mathbf{Q}$ will provide a satisfactory representation of the material. A correspondingly simple visco-elastic model can be utilized. Studies of this type have been made on the project of which this work is a part, and methods of deducing such simple approximations will be described elsewhere [9].

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Viscous Heat Effects In Extrusion of Molten Plastics

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When a fluid is forced through a capillary tube at very high shear stresses, heat is produced because of the internal friction of the fluid. Brinkman** has developed a method for solving the differential equations of motion and energy balance to describe the heat effects for newtonian flow in capillaries of circular cross-section. In this paper it is shown how Brinkman's method can be extended to describe the heat effects for the flow of non-newtonian fluids which obey a power-law relation between the coefficient of viscosity and the shear stress. The temperature profiles are calculated for one particular power law corresponding to the flow of a general purpose polyethylene melt. Calculations are made for two cases: (1) the capillary walls are maintained at the temperature of the feed, and (2) the capillary walls are thermally insulated. It is shown how the temperature profiles change as the fluid flows down the tube, and the average and maximum temperatures are calculated. The effect of the viscous heat production on the rheometer flow curves is then calculated approximately. The theory predicts satisfactorily the value of the shear stress for which the viscous heat effects begin to be important and also gives a flow curve the shape of which is in good agreement with the experimental flow curve.

National Science Foundation Grant G-356

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INTRODUCTION

When molten plastics are extruded through fine capillaries at high shear stresses, a temperature distribution can result from the heat produced by viscous dissipation. It is of interest to study the form of the resulting temperature profiles and the order of magnitude of the temperature effects, in order better to interpret the flow curves of plastics melts and also to know whether or not the temperature effects are sufficiently large to produce changes in the molecular or bulk properties of the polymers.

Molten plastics are non-newtonian fluids, and heretofore only rough calculations have been made for the heat effects in non-newtonian flow through capillaries of cylindrical cross-section. [1] The corresponding flow problem for newtonian flow has, however, been the subject of several investigations. Hersey[2] was the first to suggest that deviations from Poiseuille's law at high shear stresses could be interpreted in terms of viscous heat effects, and he gave simple explanations of the effect for both the iso-

thermal wall and the insulated wall problems. Subsequently Hersey and Zimmer[3] considered the problem from dimensional arguments. A very extensive investigation was carried out by Philippoff[4], who unfortunately based his treatment on an incorrect differential equation. Philippoff's work has been discussed in great detail by Hausenblas[5], who has considered viscous heat effects in flow through slits and tubes. Hausenblas has not, however, given any comparisons between calculated and experimental results, nor has he taken into account the transport of heat by convection. By far the most careful treatment is that of Brinkman[6], who has considered the heat convection contribution for both the isothermal wall and the insulated wall problems.

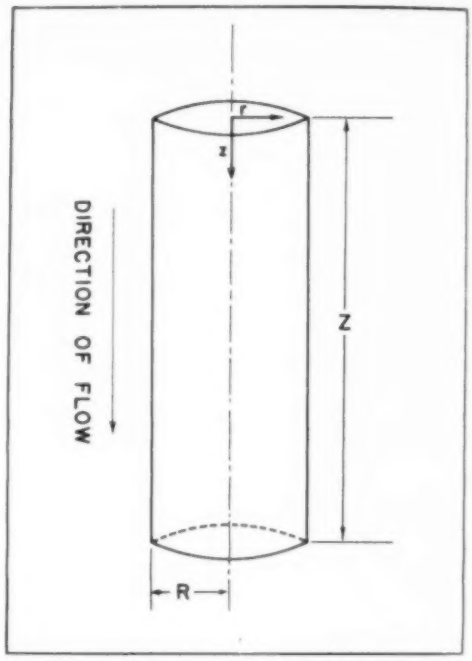
In addition to the above-mentioned treatments of flow through capillaries, there have been several other analytical solutions for flow with viscous heat effects in other simple geometries. Problems on flow in concentric cylinder viscometers have been studied by Weltmann and Kuhns[7] and summarized by Blok[8]. Flow near a rotating plate has been examined by Millsaps and Pohlhausen[9].

It is the purpose of this paper to show how Brinkman's work on flow through capillaries may be extended to describe heat effects for some types of non-newtonian flow, and to calculate the temperature profiles for one particular type of molten plastic. The effect of this temperature distribution on the flow curves for the molten plastic are considered briefly.

THE DIFFERENTIAL EQUATION FOR THE TEMPERATURE DISTRIBUTION IN NON-NEWTONIAN FLOW

The basic equations which describe the flow of a fluid in a non-isothermal system are the differential equations of continuity, motion, and energy balance, which are the statements of the principles of conservation of mass, momentum, and energy, respectively. For a fluid made up of a single chemical component, these equations in their most general form contain quantities known as the "fluxes": the momentum flux (or pressure tensor) and the energy flux. In order to utilize the basic equations, these fluxes have to be expressed in terms of the gradients of the physical properties and the transport coefficients; this is done by means of the thermodynamics of irreversible processes. Inasmuch as the basic flow equations and the expressions for the fluxes have been given recently in two readily available references[10, 11], they are not given again here.

Let us now consider the application of the basic flow equations to the problem of the extrusion of molten plastics through small capillary tubes of circular cross section at such high shear rates that the heat produced by viscous dissipation is of importance. Two problems will be considered: the **isothermal wall** problem, in which the wall of the capillary is maintained at the temperature of the molten stock; and the **insulated wall** problem, in which it is assumed that no heat can pass through the walls of the capillary tube. The geometry of the problem and the coordinates used are given in Figure 1.



1. Schematic illustration of flow in a capillary tube.

For the purpose of the mathematical development the following assumptions are made:

1. The fluid is incompressible
2. The flow is steady (all partial derivatives with respect to time are zero)
3. End effects are neglected, with respect to the development of the velocity profile
4. External forces, such as gravity, are neglected
5. The axial component of the velocity is assumed to be a function of the radial distance r alone. The radial and tangential components of the velocity are assumed zero.
6. The coefficient of shear viscosity μ is asumed to be independent of temperature and pressure, but dependent upon shear stress (and hence upon position)
7. The coefficient of thermal conductivity λ is assumed to be independent of pressure and temperature
8. Laminar flow prevails throughout
9. There is no "slip" at the walls
10. Heat transfer in the z-direction by conduction is small compared with that by convection
11. The non-newtonian nature of the fluid can be taken into account by using the ordinary newtonian expression for the momentum flux, but with the coefficient of shear viscosity taken to be some assumed function of the local shear stress.

When these assumptions are made, the equations of motion and energy balance reduce to:

$$0 = -\frac{dp}{dz} + \frac{1}{r}\frac{d}{dr}\left(\mu r \frac{dv}{dr}\right) \quad (1)$$

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$$\frac{\partial T}{\partial z} = \lambda \frac{1}{r}\frac{\partial}{\partial r}\left(r\frac{\partial T}{\partial r}\right) + \mu\left(\frac{\partial v}{\partial r}\right)^2 \qquad (2)$$

in which v is the axial velocity of the fluid, c is the heat capacity per unit volume, and T is taken to be the temperature above the stock temperature at the entrance to the capillary. In (1) the pressure gradient dp/dz may be replaced by the overall pressure drop per unit length $-P/Z$. In (2) the first term is the axial heat transfer by convection, the second term the radial heat transfer by conduction, and the last term is the heat production due to viscous dissipation, [10], [11,12].

The assumption that the viscosity is temperature independent results in considerable simplification, inasmuch as (1) and (2) are thereby uncoupled, so that the velocity distribution obtained from (1) can be used in (2) to obtain the temperature distribution. Without this assumption not only are the equations coupled, but it also becomes necessary to include both radial and axial components of the velocity in order to satisfy the equation of continuity.

In order to solve (1) for the velocity distribution, one must assume some sort of shear-stress dependence for the coefficient of shear viscosity. Many molten plastics have been shown to obey a "power-law model" over a wide range of shear-stresses. In this discussion we make use of the power law in the following form:

$$\frac{1}{\mu} = \frac{1}{\mu^\circ}\left(\frac{\tau}{\tau^\circ}\right)^\alpha \qquad (3)$$

in which μ° is the vicosity at some "standard" shear stress τ°. The quantity α is a measure of the deviation from newtonian behavior (for newtonian flow $\alpha = 0$ and μ is a constant; for non-newtonian flow $\alpha > 0$ and not necessarily integral). In circular tubes the shear stress varies linearly* with the radial coordinate r. Hence it is more convenient to rewrite (3) thus:

$$\frac{1}{\mu} = \frac{1}{\mu^\circ}\left(\frac{\tau_R}{\tau^\circ}\right)^\alpha\left(\frac{r}{R}\right)^\alpha \qquad (4)$$

in which τ_R is the shear stress at the wall of the capillary where $r = R$. This particular form of the power law is convenient because all of the quantities appearing therein have physically meaningful dimensions, and also because the last two factors give explicitly the change of fluidity with radial distance and the change from experiment to experiment (that is, as the pressuring gradient is altered). The advantages and disadvantages of the power law have been discussed in some detail by Reiner. [13]

The velocity distribution for the power law model is obtained by integration of (1a), in which the expression for μ in (4) is used. This gives:

$$v = \frac{R}{\alpha+2}\left(\frac{\tau^\circ}{\mu^\circ}\right)\left(\frac{\tau_R}{\tau^\circ}\right)^{\alpha+1}\left(1-\left(\frac{r}{R}\right)^{\alpha+2}\right) \qquad (5)$$

This expression simplifies to the usual Poiseuille velocity distribution when $\alpha = 0$. The heat produced by viscous dissipation may then be obtained from (4) and (5):

$$\mu\left(\frac{dv}{dr}\right)^2 = \frac{\tau^{\circ 2}}{\mu^\circ}\left(\frac{\tau_R}{\tau^\circ}\right)^{\alpha+2}\left(\frac{r}{R}\right)^{\alpha+2} \qquad (6)$$

From this it is clear that most of the heat produced by viscous effects is liberated in the vicinity of the wall.

*This is easily seen by integration of (1) with respect to r to give:

$$\mu\ (dv/dr) = -Pr/2Z = -\tau \qquad (1a)$$

in which τ is the local shear stress.

[10] *H. Lamb, Hydrodynamics, Sixth Edition, Dover Publications, New York, (1953)—p. 580*

We now define the following reduced variables, which reduce to those of Brinkman[6] when α is set equal to zero:

$$\rho = \text{reduced radial distance} = \frac{r}{R} \qquad (7)$$

$$\zeta = \text{reduced axial distance} = z \cdot \frac{(\alpha+2)\,\lambda\mu^\circ}{cR^3\,\tau_R\,(\tau_R/\tau^\circ)^\alpha} \qquad (8)$$

$$\theta = \text{reduced temperature rise} = T \cdot \frac{(\alpha+4)^2\,\lambda\mu^\circ}{4R^2\,\tau_R^2\,(\tau_R/\tau^\circ)^\alpha} \qquad (9)$$

In terms of these quantities the substitution of (5) and (6) into (2) gives:

$$(1-\rho^{\alpha+2})\frac{\partial\theta}{\partial\zeta} = \frac{1}{\rho}\frac{\partial}{\partial\rho}\left(\rho\frac{\partial\theta}{\partial\rho}\right) + \frac{(\alpha+4)^2}{4}\rho^{\alpha+2} \qquad (10)$$

This equation is to be solved for the temperature distribution $\theta(\rho,\zeta)$ for the two cases described above for which the boundary conditions are:

ISOTHERMAL WALL
$$\theta = 0 \text{ at } \zeta = 0 \qquad (11)$$
$$\theta = 0 \text{ at } \rho = 1$$
$$\partial\theta/\partial\rho = 0 \text{ at } \rho = 0$$

INSULATED WALL:
$$\theta = 0 \text{ at } \zeta = 0$$
$$\partial\theta/\partial\rho = 0 \text{ at } \rho = 1$$
$$\partial\theta/\partial\rho = 0 \text{ at } \rho = 0$$

For both the isothermal wall and the insulated wall problems, the solution to (10) may be written as the sum of two functions, thus:

$$\theta = \theta_1 + \theta_2 \qquad (13)$$

The first function, θ_1, is the solution to a special simplified case; these solutions are given in the next section. The function θ_2 is the additional function required to give the general solution.

SOLUTIONS TO SIMPLIFIED CASES OF THE DIFFERENTIAL EQUATION FOR THE TEMPERATURE DISTRIBUTION

It is easy to get approximate solutions to the differential equation for both isothermal wall and insulated wall problems, by making additional simplifying assumptions. The results for these special cases are then used to obtain the analytical form for the general solution in the next section.

a. **Isothermal Wall**

If it is assumed that the convection term

$$(1-\rho^{\alpha+2})\ \partial\theta/\partial\zeta$$

in (10) is unimportant, then the temperature distribution depends only on the radial coordinate. The solution of the differential equation consistent with the boundary conditions at $\rho = 0$ and $\rho = 1$ is then:

$$\theta_1 = (1/4)\,(1-\rho^{\alpha+4}) \qquad (14)$$

For newtonian fluids ($\alpha = 0$) this reduces to the fourth-power temperature distribution obtained earlier by Hersey.[2] Philippoff[4] obtained the same radial dependence, but the factor in front of his expression is incorrect by a factor of 12/16.

Hausenblas[5] has suggested a method for modifying the above result when the temperature dependence of the viscosity coefficient must be taken into account. Brinkman's calculations[6] have shown that when the convective term is taken into account, the temperature profile in newtonian flow deviates considerably from the fourth-power dependence.

b. Insulated Wall

When the conduction term in (10) is omitted and the remaining terms are averaged over the radial coordinate (by multiplying by ρ and integrating from $\rho = 0$ to $\rho = 1$), then the equation may be integrated with respect to the axial coordinate ζ to give.

$$\theta = \frac{(\alpha+4)^2}{2(\alpha+2)}\,\zeta \qquad \text{or} \qquad T = \frac{P}{c}\,\frac{z}{Z} \tag{15}$$

This result was also obtained by Hersey for newtonian fluids,[2] and states that, to a first approximation, the temperature increases linearly down the tube and depends solely on the pressure drop ρ and the heat capacity (per unit volume) of the fluid, **c**.

Substitution of (15) into (10) and subsequent integration with respect to ρ with the boundary condition that $d\theta/d\rho = 0$ at $\rho = 0$ and $\rho = 1$ gives then the radial dependence of the temperature distribution. It is then assumed that this function of ρ can be added to the function of ζ in (15) to obtain a first approximation to the temperature profile in the insulated wall problem, thus:

$$\theta_1 = \frac{(\alpha+4)^2}{8(\alpha+2)}\left(\rho^2 - \frac{2\rho^{\alpha+4}}{\alpha+4} + 4\zeta\right) \tag{16}$$

This approximate expression has been used for rough estimates of the effects of the viscous heat effects on the flow curves of plastics melts.[1]

GENERAL SOLUTION TO THE DIFFERENTIAL EQUATION FOR THE TEMPERATURE DISTRIBUTION

When the expression for θ_1 in either (14) or (16) is substituted into (13), which in turn is substituted into the differential equation (10), the following equation is obtained for θ_2 for **both** the isothermal and the insulated wall problems:

$$(1-\rho^{\alpha+2})\,\frac{\partial\theta_2}{\partial\zeta} = \frac{1}{\rho}\,\frac{\partial}{\partial\rho}\left(\rho\,\frac{\partial\theta_2}{\partial\rho}\right) \tag{17}$$

The functions θ_2 obtained from (17) are different for the isothermal and insulated wall problems, since the boundary conditions (11) and (12) are different for the two problems.

The solution to the partial differential equation (17) is of the form:

$$\theta_2\,(\rho, \zeta) = -\sum_{i=1}^{\infty} B_i\,\phi_i\,(\rho)\,\psi_i\,(\zeta) \tag{18}$$

in which the B_i are constants and the $\psi_i\,(\zeta)$ and $\phi_i\,(\rho)$ are the solutions to the following ordinary differential equations:

$$\frac{d\psi_i}{d\zeta} + a_i\psi_i = 0 \tag{19}$$

$$\frac{1}{\rho}\,\frac{d}{d\rho}\left(\rho\,\frac{d\phi_i}{d\rho}\right) + a_i\,(1-\rho^{\alpha+2})\phi_i = 0 \tag{20}$$

in which the a_i are the eigenvalues of the problem, obtained when the solutions to (20) are required to satisfy the boundary conditions. The solution to (19) shows that ψ_i is proportional to $\exp(-a_i\zeta)$. The solution to (20) may be obtained by means of a power series development; this is done in the next section for the special case that $\alpha = 1$. It is easy to show that the solutions to (20) must satisfy the orthogonality condition:

$$\int_0^1 (1-\rho^{\alpha+2})\,\rho\,\phi_i\phi_j\,d\rho = 0 \qquad i \neq j \tag{21}$$

The final expressions for the temperature distribution are:

ISOTHERMAL WALL:

$$\theta = \frac{1}{4}\,(1-\rho^{\alpha+4}) - \sum_{i=1}^{\infty} B_i\,\phi_i\,(\rho)\exp(-a_i\zeta) \tag{22}$$

INSULATED WALL:

$$\theta = \frac{(\alpha+4)^2}{8(\alpha+2)}\left[\rho^2 - \frac{2\rho^{\alpha+4}}{\alpha+4} + 4\zeta\right] - \sum_{i=1}^{\infty} B_i\,\phi_i\,(\rho)\exp(-a_i\zeta) \tag{23}$$

From these solutions it can be seen that the approximate solutions obtained in the preceding section are just the limiting solutions for large values of ζ.

The constants B_i are determined by writing (22) and (23) for $\zeta = 0$, then multiplying by $\rho\phi_j\,(1-\rho^{\alpha+2})$ and integrating from 0 to 1. In that way the following expressions are obtained:

ISOTHERMAL WALL:

$$B_j = \frac{1}{4}\,\frac{\displaystyle\int_0^1 (1-\rho^{\alpha+4})\,(1-\rho^{\alpha+2})\,\rho\,\phi_j\,d\rho}{\displaystyle\int_0^1 (1-\rho^{\alpha+2})\,\rho\,\phi_j^2\,d\rho} \tag{24}$$

INSULATED WALL:

$$B_j = \frac{(\alpha+4)^2}{8(\alpha+2)}\,\frac{\displaystyle\int_0^1 \left(\rho^2 - \frac{2\rho^{\alpha+4}}{\alpha+4}\right)(1-\rho^{\alpha+2})\,\rho\,\phi_j\,d\rho}{\displaystyle\int_0^1 (1-\rho^{\alpha+2})\,\rho\,\phi_j^2\,d\rho} \tag{25}$$

The solutions (22) and (23) for newtonian flow ($\alpha = 0$) were obtained by Brinkman[6]. Subsequently Lauwerier[14] showed that the solution for newtonian, isothermal-wall flow could be written in terms of Whittaker functions and that it is possible to obtain an asymptotic development to find the higher eigenvalues. In the next section a summary is given of the numerical results for the particular non-newtonian flow given by $\alpha = 1$. This choice for the index α corresponds to certain types of molten polyethylene plastics.

CALCULATION OF THE TEMPERATURE PROFILE (FOR $\alpha = 1$)

The eigenfunctions ϕ_i which satisfy the differential equation (20) for the particular case $\alpha = 1$ can be written as the series:

$$\phi_i = \sum_{k=0}^{\infty} b_{ik}\,\rho^k \tag{26}$$

in which b_{i0} is arbitrarily chosen to be unity. The coefficient b_{i1} must be zero to satisfy the boundary condition at $\rho = 0$. Substitution of (26) into the differential equation (20) gives the following recursion formula for the b_{ik}:

$$b_{ik} = -(a_i/k^2)\,(b_{i,\,k-2} - b_{i,\,k-5}) \tag{27}$$

Hence all of the b_{ik} can be expressed in terms of the eigenvalues a_i

$$b_{i2} = -(1/4)a_i,\ b_{i3} = 0,\ b_{i4} = (1/64)a_i^2,\ b_{i5} = (1/25)a_i,\ b_{i6} = (1/2304)a_i^3,\ b_{i7} = -(29/4900)a_i^2,\ \text{etc.}$$

The eigenvalues a_i are determined from the boundary conditions at $\rho = 1$ which require that:

[1] *M. Reiner, "Deformation and Flow," Lewis, London (1949)—Chapter 6 and 7.*

[14] *H. A. Lauwerier, Appl. Sci. Res., A2, 184-204 (1951)*

ISOTHERMAL WALL: $$\sum_{k=0}^{\infty} b_{ik} = 0 \qquad (28)$$

INSULATED WALL: $$\sum_{k=0}^{\infty} k\, b_{ik} = 0 \qquad (29)$$

The values of the a_i found herefrom are listed in Table 1. With these a_i the b_{ik} can be calculated from (27) and thereby the eigenfunctions are obtained from (26); the first three eigenfunctions are tabulated in Table 2. The coefficients B_i are then obtained from (24) and (25); the values calculated by numerical integration are given in Table 1.

TABLE 1

Constants for Use in Equations (22) and (23) when $\alpha = 1$

i	Isothermal Wall		Insulated Wall	
	a_i	B_i	a_i	B_i
1	6.582	+0.349	0	+0.3125
2	39.09	−0.147	22.43	−0.413
3	99.50	+0.057	73.10	+0.146
4	187.9	−0.014*	151.9	−0.040*

*Estimated values

TABLE 2

The Eigenfunctions ϕ_i for Use in Equations (22) and (23) when $\alpha = 1$

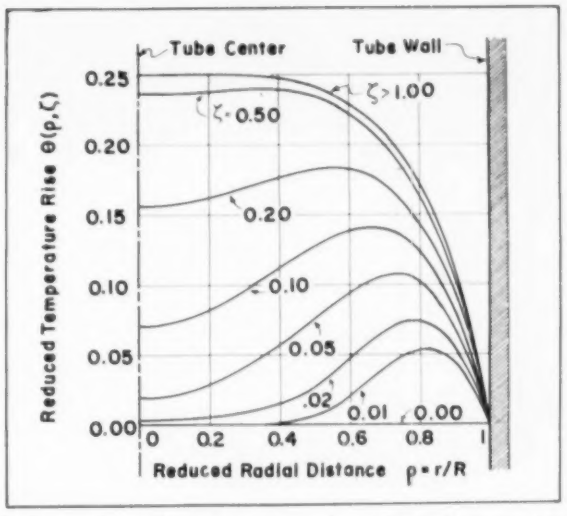
ρ	Isothermal Wall			Insulated Wall	
	ϕ_1	ϕ_2	ϕ_3	ϕ_2	ϕ_3
0.0	1.000	1.000	1.000	1.000	1.000
0.1	0.988	0.884	0.766	0.945	0.825
0.2	0.935	0.646	0.228	0.789	0.393
0.3	0.855	0.310	−0.257	0.550	−0.079
0.4	0.756	−0.045	−0.403	0.290	−0.369
0.5	0.638	−0.311	−0.209	0.020	−0.385
0.6	0.506	−0.414	0.118	−0.187	−0.181
0.7	0.374	−0.385	0.310	−0.340	0.082
0.8	0.236	−0.293	0.310	−0.434	0.295
0.9	0.110	−0.166	0.185	−0.469	0.375
1.0	0.000	0.000	0.000	−0.470	0.390

Note: ϕ_1 for the insulated wall problem is 1.000 for all values of ρ.

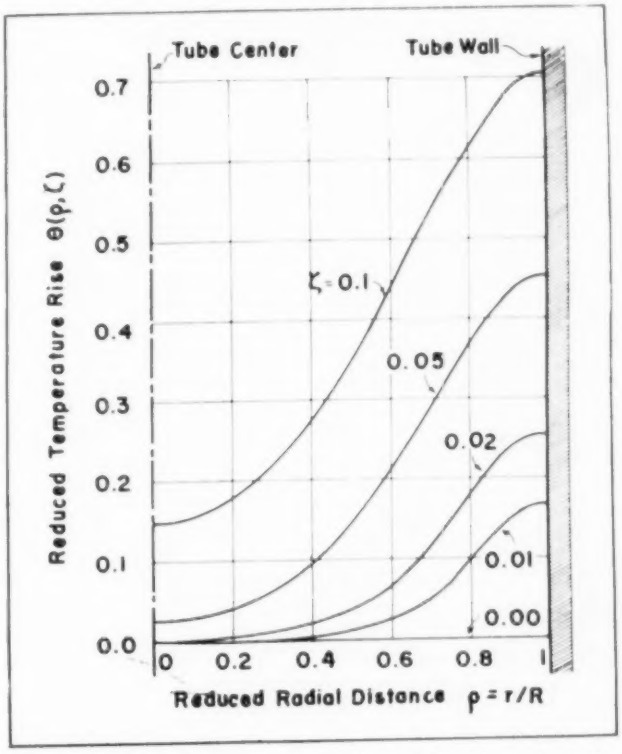
From the expressions given in (22) and (23) for the temperature distribution and the information given in Tables 1 and 2, one can compute the temperature profiles as a function of the reduced axial distance ζ. Figures 2 and 3 show graphically the development of the temperature distribution in both isothermal wall and insulated wall flow. These curves allow one to specify the value of ζ for which the exponential terms in (22) and (23) have virtually ceased to contribute to $\phi(\rho, \zeta)$—that is, a "Temperatureinlauflaenge," ζ_T: for the isothermal wall $\zeta_T = 1$ and for the insulated wall $\zeta_T = 0.02$. The average temperature and the maximum temperature for each of the temperature profiles are given in Table 3.

ESTIMATION OF EFFECT ON VISCOUS HEAT PRODUCTION ON FLOW CURVES

For shear stresses up to about $\tau_R = 10^6$ dyn cm^{-2} many plastics melts behave according to the power law (9), so that rheometer extrusion data are usually represented by straight lines on plots of log τ_R vs. log D (in which $D = 4Q/\pi R^3$, the quantity being the volumetric discharge rate from the capillary). Measurements by Tordella[15] on an "Alathon" 1 polyethylene resin above $\tau_R =$



2. Profiles of the reduced temperature for a fluid with $\alpha = 1$ flowing in a tube whose walls are maintained at constant temperature.



3. Profiles of the reduced temperature for a fluid with $\alpha = 1$ flowing in a tube with thermally insulated walls.

10^6 have shown deviations from the linear flow curves. One such curve is reproduced in Figure 4. In this section we obtain an approximate expression for the deviation from the linear flow curves in terms of the viscous heat effects.

It is first necessary to specify the temperature variation of the viscosity. For this purpose we modify (4) by writing:

$$\frac{1}{\mu} = \frac{1}{\mu^0}\left(\frac{\tau_R}{\tau^0}\right)^a \left(\frac{\tau}{R}\right)^a (1 + \beta_1 T + \beta_2 T^2) \qquad (30)$$

in which T is the temperature rise above T^0 the tempera-

[15] *J. P. Tordella, Society of Plastics Engineers Journal, 9, 6-9 (1953)*

ture of the stock solution entering the capillary, and μ^0 is that value of the (true) viscosity at the standard shear stress τ^0 and at the stock temperature T^0; the quantities β_1 and β_2 are constants characteristic of the melt. A quadratic dependence of the fluidity on temperature is adequate over a range about 200°C.

An expression may now be obtained for the velocity distribution by integrating (1), using the expresion for μ given in (30) and the expresions for the temperature profile in (22) and (23). This gives a modification of (5):

$$v = \frac{R\ \tau^0}{\alpha+2\ \mu^0}\left(\frac{\tau_R}{\tau^0}\right)^{\alpha+1}\left[(1-\rho^{\alpha+2})+(\alpha+2)\beta_1\ K\int_\rho^1 \rho^{\alpha+1}\,\theta\,d\rho\ +\ (\alpha+2)\ \beta_2\ K\int_\rho^1 \rho^{\alpha+1}\,\theta^2\,d\rho\right] \qquad (31)$$

in which $K = 4R^2\ {\tau_R}^{\alpha+2}/(\alpha+4)^2\ \lambda\mu^0\ {\tau^0}^{\alpha}$

The velocity distribution so obtained is a function of both the radial and axial cordinate, in contradiction to assumption 5 above. Therefore this expression for the velocity distribution must be regarded as an approximation.

The function $D\ (\tau_R)$, which gives essentially the flow rate as a function of the pressure, is then obtained from the velocity distribution thus:

$$D = (4Q/\pi R^3) = (4/\pi R^3)\int_0^R v\ 2\pi r\ dr = (8/R)\int_0^1 v\ \rho\ d\rho \qquad (32)$$

in which v is the velocity distribution at z = Z. Substitution of 31 into (32) then gives the result:

$$D/D_0 = 1 + V_1 W_1\ {\tau_R}^{\alpha+2} + V_2 W_2\ {\tau_R}^{2(\alpha+2)} \qquad (33)$$

in which $D_0 = 4{\tau_R}^{\alpha+1}/(\alpha+4)\mu^0{\tau^0}^{\alpha}$

is the flow rate which the fluid would have in the complete absence of viscous heat effects; and the V's and W's are:

$$V_1 = \frac{4R^2\ \beta_1}{(\alpha+4)\ (\mu^0{\tau^0}^{\alpha})\lambda} \qquad (34)$$

$$V_2 = \frac{16R^4\ \beta_2}{(\alpha+4)^3\ (\mu^0{\tau^0}^{\alpha})^2\ \lambda^2} \qquad (35)$$

$$W_1\ (\zeta_Z) = \int_0^1 \rho^{\alpha+3}\ \theta\ (\rho,\zeta_Z)\ d\rho \qquad (36)$$

$$W_2\ (\zeta_Z) = \int_0^1 \rho^{\alpha+3}\ [\theta\ (\rho,\zeta_Z)]^2\ d\rho \qquad (37)$$

in which ζ_Z is the value of ζ for z = Z. Hence the deviation from isothermal behavior can be expressed simply in terms of the shear stress at the wall τ_R, the factors V_1 and V_2 (which contain all the physical constants of the fluid), and the weighted integrals over the temperature distribution W_1 and W_2. The latter quantities are tabulated in Table 3, for $\alpha = 1$.

§7. Numerical Results for a General Purpose Polyethylene Melt ($\alpha = 1$)

In order to compare the theoretical results with the experimental flow curve of Tordella[15] shown in Figure 4, the following numerical values were used:

Dimensions of Apparatus

R = radius of capillary tube = 0.0394 cm
Z = length of capillary tube = 1.28 cm

Standard Quantities

T^0 = standard (stock) temperature = 190°C
τ^0 = standard shear stress = 10^5 dym cm^{-2}

Physical Properties of Melt

c = specific heat per unit volume* = 0.43 cal cm^{-3} deg^{-1}
λ = thermal conductivity in neighborhood of T^0 ** = 10^{-4} cal cm^{-1} sec^{-1} deg^{-1}
μ^0 = (true) viscosity at τ^0 and T^0 † = 4.76 x 10^{-6} gm cm^{-1} sec^{-1}
α = index for power law shear stress dependence of viscosity † = 1.0

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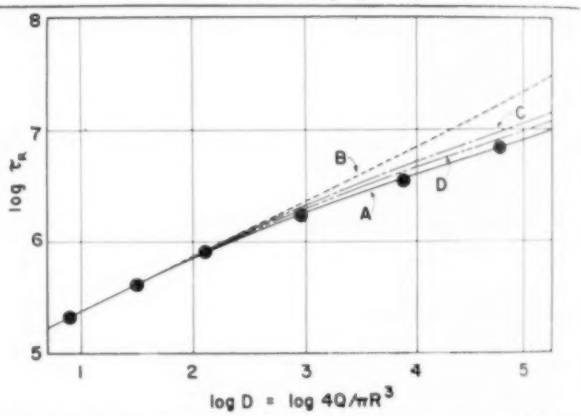

4. Experimental and theoretical flow curves for an "Alathon" 1 Polyethylene resin at 190°C. The four curves shown are: (A) Experimental curve with points measured by J. P. Tordella[15] at 190°C. (B) Extrapolation of experimental curve based on experimental data at shear stress less than 10^6. (C) Calculated flow curve based on "insulated" model for capillary flow. (D) Calculated flow curve based on "isothermal" model.

TABLE 3

Tabulation of θ_{av}, θ_{max}, W_1 and W_2 for Non-Newtonian Fluids with $\alpha = 1$

	Isothermal Wall				Insulated Wall			
	θ_{av}	θ_{max}	W_1	W_2	θ_{av}	θ_{max}	W_1	W_2
0.001*	0.007	0.022	0.0026	0.000039	0.010	0.048	0.0044	0.00014
0.002*	0.011	0.029	0.0035	0.000072	0.018	0.070	0.0072	0.00035
0.005*	0.018	0.042	0.0052	0.000165	0.039	0.115	0.0140	0.00122
0.01	0.028	0.056	0.0071	0.000308	0.071	0.171	0.0230	0.00318
0.02	0.043	0.074	0.0097	0.000570	0.126	0.258	0.0384	0.00827
0.05	0.073	0.108	0.0141	0.00121	0.275	0.455	0.0752	0.0299
0.1	0.104	0.140	0.0177	0.00194	0.498	0.707	0.124	0.0792
0.2	0.140	0.183	0.0212	0.00290	0.922	1.14	0.211	0.225
0.5	0.173	0.239	0.0245	0.00398	2.17	2.40	0.461	1.066
1.0	0.179	0.250	0.0250	0.00417	4.25	4.48	0.878	3.85

*The values of θ_{av}, θ_{max}, W_1 and W_2 for $\zeta < 0.01$ were obtained by extrapolation using an exponential function of the form A exp $(-B\zeta)$.

Dynamic Investigations on Polymeric Solutions Using Different Methods

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and

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Dynamic testing of polymer solutions has been performed over a number of years by several laboratories using completely different experimental procedures. Up to now, it has not been possible, except in the case of the solid polyisobutylene in the Bureau of Standards' cooperative program, to ascertain whether these different methods give the same values for the same polymer solutions; in other words, whether the values of the dynamic modulus, G' and the loss modulus, G" are real constants of the material and therefore independent of experimental method. We are reporting here a series of measurements made on a single solution, 20% NBS polyisobutylene in decalin, using three completely different methods in two laboratories, all having the stressing in **shear** in common:

1. Ferry's method of wave propagation as described[1] in 1941 and 1942. These measurements were performed at the Department of Chemistry, University of Wisconsin.
2. A method used at The Franklin Institute for measuring the force and displacement together with the phase angle, using an oscilloscope and double transducers as described[2] in 1953 by W. Philippoff.
3. A modification of Mason's method[3] of the vibrating torsion crystals as described by Rouse and Sittel[4] at the Franklin Institute.

Whereas Ferry's method does not involve any assumptions except the measurement of the velocity of the shear wave and its attenuation by **optical** means in an infinitely extended solution, the transducer method of Philippoff involves a more complicated velocity distribution, and calibration of the force and deformation in absolute units. The measurement is basically **mechanical.** By the method of Sittel, the impedance offered by the solution to a crystal vibrating at resonance is measured **electrically.** The frequency range and the maximum amplitude obtainable by these methods are seen in Table I.

TABLE 1

Method	Frequency Range (cps)	Maximum Shear Amplitude (%)
Ferry (1)	100 to 800	<5
Philippoff (2)	3 x 10^{-5} to 10	130
Sittel (3)	20 x 10^{3} (not a "range")	10^{-5}

The experiments at the University of Wisconsin were part of a program of research on the physical structure and properties of cellulose derivatives and other polymers, supported by the Allegany Ballistics Laboratory, Cumberland, Maryland, an establishment owned by the United States Navy and operated by the Hercules Powder Company under Contract NOrd 10431.

[1] *J. D. Ferry, Rev. Sci. Instr., 12:79 (1941). T. Amer. Chem. Soc. 64 1323, 1330 (1942).*
[2] *W. Philippoff, J. Appl. Phys., 24:685 (1953).*
[3] *W. P. Mason, Trans. Am. Soc. Mech. Eng., 69:359 (1947).*
[4] *P. E. Rouse and K. Sittel, J. Appl. Phys., 24:690 (1953).*

It is obvious that the range of amplitudes is nearly 1 to 10^7. Using the results reported by Philippoff[5], one could expect the dynamic properties to be independent of amplitude. This cooperative work allows us to prove it in an otherwise inaccessible range of 1 to 10^7. Only if the properties are independent of the amplitude, could one expect the results from these three different methods to fit the same curve. The evaluation of the complex shear modulus (G) in dynes/cm², the phase angle (δ) between force and deformation, as well as the real part of the shear modulus (G') and the loss modulus G" (=viscosity $x\omega$) is given in the next paragraph. (ω is the circular frequency of vibration.)

RESULTS WITH METHOD 1

Ferry's method of wave propagation is evaluated according to the equations: [6]

$$G' = \rho \lambda^2 f^2 \frac{4\pi^2 [4\pi^2 - (\lambda/X_\alpha)^2]}{[4\pi^2 + (\lambda/X_\alpha)^2]^2}$$

$$G'' = \rho \lambda^2 f^2 \frac{16\pi^3 (\lambda/X_\alpha)}{[4\pi^2 + (\lambda/X_\alpha)^2]^2}$$

where ρ is the density of the solution in g/cc
λ is the wave length in cm
f is the frequency in cps
X_α is distance in direction of propagation when amplitude decreases 1/e = 0.368

The measurements were made at 0, 25 and 50° C. Measurements of the wave length are essentially much more exact than the measurements of attenuation (X_α) so that the measurements of G' have much smaller errors than those of G".

RESULTS WITH METHOD 2

Measurements made with the double transducer are evaluated by calibrating the oscillographically recorded hysteresis loop in **kg** and microns (μ) and evaluating the loop directly as double as maximum force (=2F) in **kg**, double the maximum displacement (2D) of the inner cylinder in μ, and breadth of the ellipse **a** at D=0. The measurement thus gives, in the well known way,

$$G = 0.715 \times 10^6 \, F/D \text{ dynes/cm}^2$$

and

$$\sin \delta = a/2F$$

The instrument used for these measurements has not been described in detail. It consists of concentric cylinders where the inner cylinder is axially displaced with respect to the outer one by an amount ± **D** and the liquid is pumped through the annulus as the inner cylinder moves up and down. The formulas involved have been developed by a series of investigators, for example Smith, Ferry, and Schremp.[7] The inner diameter of the outer

[5] *W. Philippoff, J. Appl. Phys., 25:1102 (1954).*
[6] *J. D. Ferry, W. M. Sawyer and T. N. Ashworth J. Polymer Sci. 2 593 (1947).*

cylinder was 77.80 mm and the outer diameter of the inner cylinder 65.00 mm. The equivalent length of the inner cylinder determined by calibration was 47.00 mm. The constant 0.715 x 10^6 is calculated from the geometrical dimensions and checked by calibration with a Bureau of Standards normal oil of about 200 P viscosity at 25°C. The maximum shear at the well of the inner cylinder is $\gamma = 0.00287$ D with D in microns. The measurements were performed at temperatures of +25, 0, and −23°C. Higher temperatures were not used as a noticeable change in concentration due to evaporation of the solvent was observed in preliminary runs.

RESULTS WITH METHOD 3

Mason's method of the torsion crystal was recently applied to solid rubber samples by K. Sittel and P. Rouse.[4] Sittel has shown* that it can also be adapted for measurements on viscous solutions by placing the end of the crystal (10 mm diameter and 100mm length) in the plane surface of the liquid. As long as the wave length of the shear wave in the liquid is only a small fraction of the diameter of the crystal, the shear wave can be considered as a plane wave traveling in an infinite medium. In other words, the wave propagation is assumed to be confined within a cylindrical volume which extends from the crystal into the liquid. The crystal touching the surface of the test liquid is unsymmetrically loaded. It can be shown that this has a negligible influence on the wave distribution along the crystal so that the results can be treated in the same fashion as in the case of a symmetrically loaded crystal if the change of the resonant frequency and the electrical resistance between the loaded and unloaded crystal is doubled.

When the crystal is brought in contact with the liquid, the liquid will not only wet the flat end surface, but due to the surface tension, rise on the sides of the crystal. This capillary rise of a liquid at the circumference of a cylinder is derived by Ferguson[8], assuming complete wetting. In our measurements, the capillary rise of the solution has been experimentally determined; it was approximately 0.025 cm ten minutes after contact with the viscous solution was first established, and approached a height of 0.13 cm after approximately two hours. In the case of the solvent the liquid rose almost instantly to its final height of 0.13 cm. As the height is only a small percentage of the crystal length, the effect is merely to add a load to the end of the crystal without taking into account the change of the wave distribution along the crystal. To establish a definite contact angle and thereby decrease the height of rise of the liquid, a thin film of silicone grease was applied in a narrow ring around the end of the crystal. To avoid errors due to the less-than-perfect crystal surface, the crystal was calibrated with a Bureau of Standards normal oil (viscosity = 0.7223 poise, and density = 0.8682 g/cm³ at + 25°C) in the same way as described above. Then the solvent and the solution were measured consecutively. The capillary rise of the liquids was determined each time and all measurements reduced to the contact with one flat end.

The measurements are evaluated according to the equations:

$$G = \frac{1}{\rho_1} \frac{l_2^2 \rho_2^2}{4} \left[\frac{\Delta R^2}{4 L_2^2} + \Delta \omega^2 \right]$$

$$\tan \delta = \frac{1}{L_2} \frac{\Delta \omega \Delta R}{(\Delta R^2/4 L_2^2) - \Delta \omega^2}$$

where ρ_1 is the density of the solution in g/cm³
ρ_2 the density of the crystal in g/cm³
l_2 the length of the crystal in cm
L_2 the effective inductance of the crystal, which is determined experimentally in henrys
ΔR the difference in the electrical resistance between the loaded and unloaded crystal in cm
$\Delta\omega = 2\pi \Delta f$ where Δf is the difference in the resonant frequency between the loaded and unloaded crystal in cps.

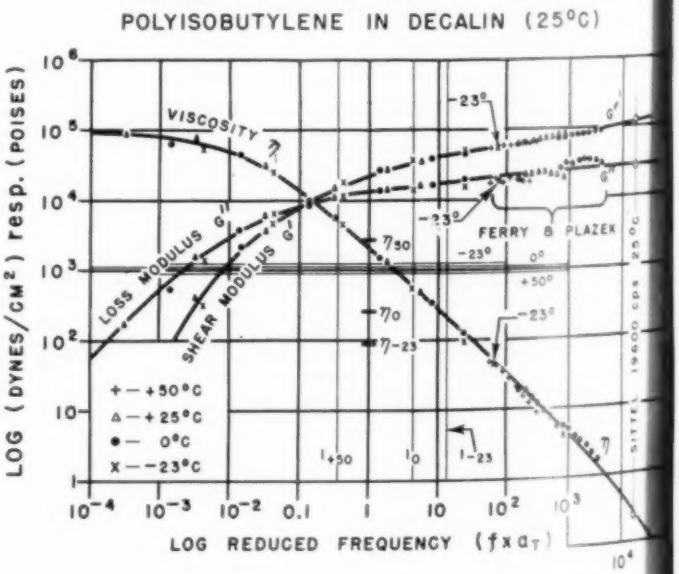
We see that under these conditions (plane wave and infinite sample), the dimensions of the specimen do not enter into the calculations, and only electrical quantities and Δf are used. In contrast to the low-frequency measurements these measurements were made at very low shearing strains. The shear strain can be easily calculated from the resistance of the loaded crystal, the applied voltage and the attenuation constant. For the solution under test, the maximum shearing strain $15\gamma = \frac{d(r\theta)}{dZ}$

r being the radius of the crystal, θ the maximum angular displacement, and Z the axial coordinate. $\gamma = 1.0 \times 10^{-7}$. Measurements were made at 19.6 kc and 25°C. For these conditions the dynamic shear modulus of the 20% isobutylene solution was determined as $G' = 1.3 \times 10^7$ dynes/cm² tan $\delta = 0.21$.

COMPARISON OF RESULTS

The results of the measurements with the three different methods are shown in Figure 1. The measurements at different temperatures were reduced by the a factors of Ferry to form a single reduced curve at 25°C. a_T As can be seen from the measurements, the lowest frequency in method 2 was measured at 25°C. Sittel's measurements were also made at 25°C so that the curves give the dispersion of the mechanical properties of the solution in the range of about eight decades of frequency under isothermal conditions. The values at other temperatures fit in nicely with these measurements at 25°C. The G' curve is continuous in the whole range investigated.

The conclusions to be drawn from the curve are ob-



[7] *T. L. Smith, J. D. Ferry and F. W. Schremp J. Appl. Phys., 20 144 (1949).*

**K. Sittel, to be published.*

[8] *Wien Harms, Handbuch d. Exp. Physik., Vol. VI, 1928, p. 95.*

vious. The dynamic properties of the solution are the real constants independent of the method used. The amplitude has no measurable influence on the values even over a range of nearly 1 to 10^7. The general shape of the curve is similar to the one obtained by Ferry for other polymer solutions. (See also W. Philippoff[9].) Especially important is the merging of the viscosities at low frequencies to a constant value; the same value of 80,000 poise being measured with a rotational viscometer at very low rates of shear below 0.003 sec^{-1} (see W. Philippoff[5]). The validity of Ferry's method of reduced variables is again demonstrated under the most stringent conditions possible; namely, fitting an isothermal curve in the whole range of frequencies with measurements at higher and lower temperatures.

These results add substantial weight to the dynamic experiments, proving that the dynamic modulus and viscosity are in fact constants of the material.

[9] *W. Philippoff, Physikalische Zeitschr. 35:900 (1934).*

SUMMARY

These measurements show that a determination of the dynamic properties of polymer solutions could be obtained in eight decades of frequency at one temperature. Amplitude effects if present are of minor importance. The three completely different methods compared give absolute values entirely comparable with each other. Ferry's method of reduced variables has been substantiated.

Low Shear Viscometry....

THE SQUIBB VISCOMETER

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The need for an instrument capable of providing low shear viscometry of suspension has been felt in studies on the structural features of printing inks (6) and caking tendencies of certain pharmaceutical dispersions which are underway in this laboratory. Interest has centered on the determination of the importance of secondary structures using a combination of both low shear and high shear data as detailed by Reiner (8).

In the case of the pharmaceuticals, which in this case were aqueous dispersions, high shear data have been obtained by means of a pressurized capillary viscometer; low shear data could not be obtained with any existing fundamental instrument. Therefore, a rotational viscometer was designed for the low shear range. The versatility of the instrument is illustrated herein for systems ranging from water to printing inks and at shear rates from 1 to 600 reciprocal seconds.

The detection of secondary structures is only one of several uses of rotational viscometry. Reiner (8) has provided a list of criteria of structural features of fluid dispersions, among which are the temperature dependence of the relative viscosity and peculiarities in the shape of the flow curve. These measurements have been taken by various workers (5, 6) on dispersions at moderate shear rates, but information on low shear anomalies has not yet been reported to an extent which permits an interpretation of the mechanism of the anomaly.

Many of the reported studies have failed to yield significant data because they have been conducted at shear rates at which much of the structure of the dispersion has been broken down. One of the first instruments for the measurement of stresses at extremely low shear rates was constructed by Hayden and Schweyer (4), and adapted to materials of low viscosity by Bergen and Patterson (3); however, these instruments suffered from end effects which introduced variable shear rates and reduced their effectiveness in the measurement of non-Newtonians.

Numerous details of the final design of the viscometer were supplied by the Squibb Institute for Medical Research, a division of the Olin Mathieson Chemical Company. The prototype used in this research was constructed by the Squibb Institute.

DESIGN OF THE VISCOMETER

1. **Schematic Diagram** — A diagram of the essential features of the viscometer is given in Figure 1. The heart of the instrument is a calibrated wire which is gripped by a pin vise at its lower end and by torsion head at the upper extremity. At the midpoint of the wire is attached a stainless steel seamless ring equipped with three tabs attached by radial connecting arms to a pin vise concentric with the ring. The pin vise is clamped to the torsion wire at a point halfway between the torsion head and the lower pin vise of the frame, thus providing a free suspension which can be rotated or oscillated without friction since no bearings are employed in the torque measuring assembly.

In the model used in this investigation the wire span is 27 inches; the ring diameter is 1.3 inches and its height is 0.5 inch.

A double-walled cup concentric with the wire and the ring can be raised so that the ring fits into the annular-space of the cup. Arrangements of this type, called bell-trough assemblies, have been reported previously (1, 7, 9); however, their use in connection with the completely frictionless suspension is necessary in order to allow the wire to pass completely through the shear assembly. Three cups are currently in use, providing clearances ranging from 0.03 to 0.20 inches.

The longer torque arm and the existence of shear on both walls of the ring increase the sensitivity of the viscometer, while the provision for taking all readings at the same positioning of the ring alleviates the condition common to rotational viscometers of a variable sensitivity dependent on the angular displacement of the torsion member. End effects are virtually absent because they have been confined to the thickness of the shell rather than to its entire diameter as in conventional rotational viscometers. These second order end effects are further minimized by equipping the ring with the tabs mentioned above which elevate the points of attachment to the pin vise above the liquid level.

2. Details of Operation — A wire of suitable torsion constant is fixed to the tension gage and then threaded through the torsion head, the ring and pointer, and the cup, and is finally clamped in the pin vise which is lowered until the wire is at the desired tension. After the torsion head is set at zero and clamped to the wire, the ring and cup are carefully positioned by means of an adjustment on the lower pin vise; the final zero adjustment is made by moving the target in line with the pointer.

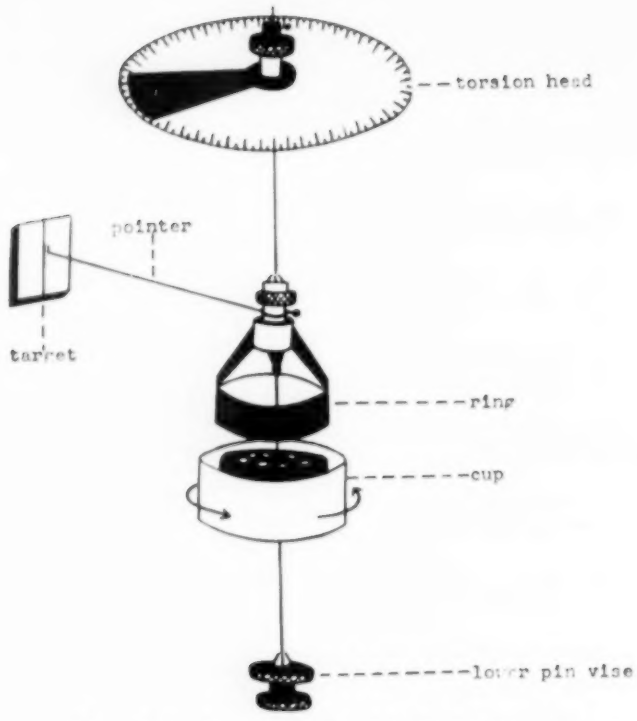

Figure 1. A schematic drawing of the Squibb viscometer

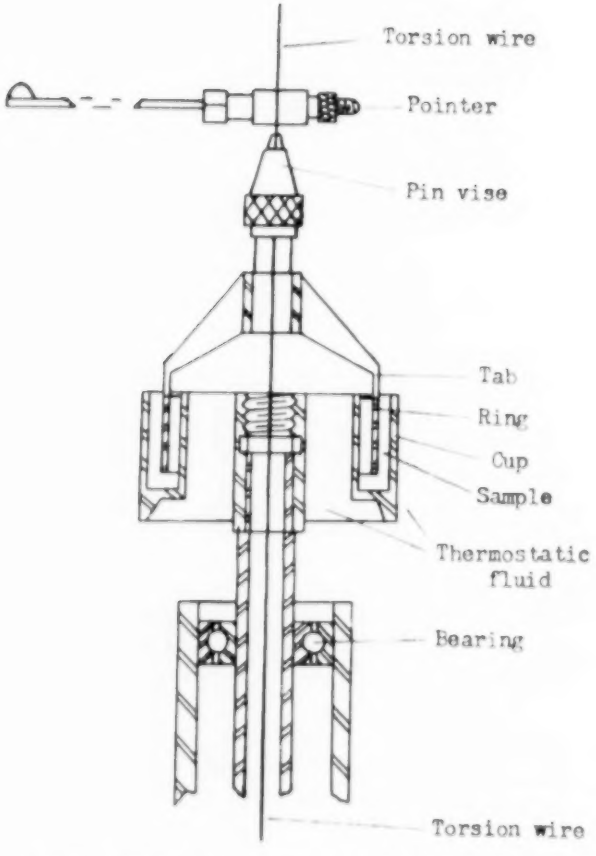

Figure 2. Scale drawing showing detail of the viscometer.

The ring immersion is adjusted to a reproducible value by means of a depth indicator supplied with the instrument. The cup is filled with the liquid or dispersion and is set into motion at a rate which is read on the tachometer. A couple is transmitted through the fluid to the ring which is thereby caused to rotate until the couple exerted on the wire equals the restoring couple due to the rigidity of the wire. The tangential force acting on the ring is calculated from the angle θ through which the torsion head vernier has to be turned to bring the pointer back to the zero mark, together with a knowledge of the wire constant and the dimensions of the ring.

The cup is operated at rotations from 1.0 to 660 rpm by means of a variable speed dc motor. Features of the viscometer which are not shown in the schematic diagram, Figure 2, include the constant temperature bath, a tension gage to ensure that the wire is always under the tension at which it was calibrated, a depth gage for adjusting the ring immersion, and a tachometer.

The use of cups of different dimensions and wires of different torsion constants enables flow curves to be obtained for a wide variety of systems. Systems ranging in viscosity from 0.01 to over 200 poises have been investigated extensively at shear rates from 1.0 to about 100 sec^{-1}, with occasional extensions to higher rates.

EVALUATION OF THE INSTRUMENT

1. Wire Calibration — The measured quantities in rotational viscometry are the revolutions of the cup per unit time and the deflection of the torsion member. Three methods of determining the torsion constants of the wires and filaments used in the present study are described below.

- **a.** Direct measurement of torque. With the viscometer lying on its side so that the torsion wire is horizontal, the pointer can be used as a lever for the application of known torques by means of small weighted segments of chromel wire.
- **b.** Indirect measurement of torque. The inconvenience of laying the viscometer on its side during calibration has led to the use of the method described by Barr (2) in which a thread supporting a balance weight is deflected from the vertical by the pointer. In the Squibb viscometer the deflection is brought about by adjustment of the torsion head whose reading is equated to the horizontal component of the weight.
- **c.** Oscillation period. The third method is based on the timing of the period of oscillation of the ring as it hangs free of the cup and its contents. A similar alternative is to use a torsion bob of known moment of inertia (2) in order to secure absolute values of the torsion constant rather than the relative values obtained by the use of the ring.

The calibration results from the oscillation method agree most closely with the calibrations supplied with certain of the wires by the MacMichael Company and with the rated viscosities of a group of standard oils used in the extensive calibration of the various wires.

2. Instrument Calibration — The precision of the instrument was evaluated by four operators working independently, using a Hartman-Leddon 14.1 cp. standard oil. When Cup #1 (trough width 0.24 cm, 4.0 ml capacity) was used in conjunction with a wire whose torsion constant was 23.1 dyne cm/deg, the average viscosity was determined by the four observers to be 13.4 with the precision extremes listed in Table 1.

Table 1. Calibration of Viscometer at 25°C Using 14.1 cp Oil. Cup #1

Operator	Shear Range (Sec^{-1})	No. of Detns.	ηAvg.	Standard Deviation of S
1	12.5 to 118	18	13.7	0.21
2	9.0 to 108	15	13.1	0.15
3	5.0 to 108	19	13.1	0.15
4	7.7 to 120	17	13.7	0.26
		Avg.	13.4	0.19

For calculation of the precision, the slope of the best line through all of the points was taken as the true viscosity, while the intercept which was finite in each case was considered to be a natural outcome of the least squares technique in which equal weight was attached to all the points. Deviations in S appeared to be the same at low shear rates as at high ones.

The accuracy of the viscometer can be seen to be affected by a negative bias of 0.7 units in the slope which was 13.4 instead of 14.1 in this case; the error, therefore, is 5%. This figure probably can be lessened by machining the cup and ring to closer tolerances or by improved alignment.

The problem of cup-ring alignment was the most serious one encountered in calibrations using the small cup, but disappeared when a cup of larger capacity, (Cup #2: 9.5 ml, 0.556 cm), was used. In this case, small departures from concentricity produced negligible effects on the flow curve.

Water was the first calibration fluid to be studied, with the result that a Newtonian flow curve was obtained at shear rates up to about 100 sec^{-1} in Cup #1. After about 50 sec^{-1}, a pronounced increase in slope occurred with Cup #2. The most likely cause is the onset of turbulence which has been calculated from Taylor's formula (2) to lie near the 50 sec^{-1} value found experimentally. Without doubt, turbulence is one of the most serious limitations to the use of the Squibb viscometer but fortunately the problem is confined to a highly specific set of conditions: namely, the high shear rate viscometry of materials of low viscosity in a cup of relatively large clearance. Since the attainment of high shear rates is more readily accomplished in cups of small clearance, this combination of requirements is not likely to be encountered.

EVALUATION OF TYPICAL SYSTEMS

1. Aqueous Procaine Penicillin Suspension — The proprietary formulations of procaine penicillin are aqueous dispersions whose vehicle, or menstrum, is a complex mixture of soluble and micellar ingredients formulated to be compatible with body fluids. The menstrum for the system reported here had a viscosity of 2.7 cp as determined by both the Ostwald and Squibb viscometers. Other commercial menstrums have viscosities which depart from this figure by a wide margin. In each case, a knowledge of the menstrum viscosity under the conditions of shear encountered in the Squibb viscometer was necessary in order that relative viscosities of the blends could be calculated.

In the case of the procaine penicillin suspensions, however, a considerable amount of interest centered around the shape of the flow curves of fresh and aged samples.

In general the blends were Newtonian. As in the case of the menstrums, the range of viscosities encountered with 30% procaine penicillin blends (300,000 units/ml) made in the laboratory and purchased on the open market was enormous. Variations from 4 to 60 cp were not uncommon. Most of this variation arose from differences among menstrums.

One important finding which low shear viscometry produced was in connection with the measurement of the viscosities of a selected 30% blend at 15° and 35°. During this temperature interval the viscosity in the range from 1.0 to 50 sec^{-1} dropped only from 17 to 16 cp.; in this same span of temperature the viscosity of water is reduced by 30%. Since the relative viscosity increases with temperature, an application of Reiner's criterion (8) causes one to arrive at the conclusion that competition exists between orientation of the particles due to flow and randomness as caused by Brownian movement. That this explanation can account for all of the measured effect is doubtful; at any rate, the kinetic unit of the dispersion appears to be larger at the higher temperature.

The Squibb viscometer was useful in observing the rate of an apparent buildup of a thixotropic structure after a procaine penicillin blend has been worked at a high rate of shear. This behavior is apparent from Figure 3, which shows the rise in measured viscosity with time after the suspension had been subjected to shear rates above 10,000 sec^{-1} before the start of the test. Buildup curves of this nature have been duplicated several times by the simple expedient of filling the viscometer by means of a hypodermic needle, during which operation the high shear rate is achieved. The phenomenon shown in Figure 3 has been explained satisfactorily only in terms of the breakdown in structure suffered by the suspension as it passes through the needle and its subsequent buildup in the viscometer. An observation of this nature requires the application of a low shear rate (11 sec^{-1} in this case).

2. Calcium Carbonate Dispersions — Another field of investigation in which the Squibb viscometer has been of value is in the viscometric analysis of model systems for printing inks. One series of dispersions was formulated by milling precipitated calcium carbonate with Triol 230, a 29 poise branched trihydroxy ether produced by Carbide and Carbon Chemicals Co., and diluted with water to increase the polarity; a comparison series was made using a 5 poise polybutene, a non polar hydrocarbon of molecular weight 250-300 provided by Indoil Chemical Co. The chief difference between the two systems is that the polybutene dispersions produced flow curves with an extensive linear range, while the flow

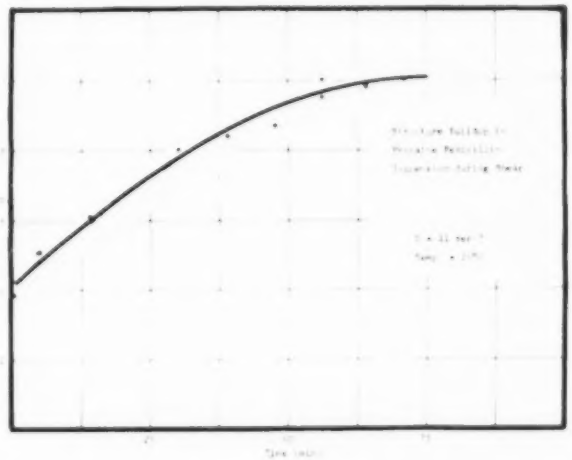

Figure 3. Structure buildup during shear.

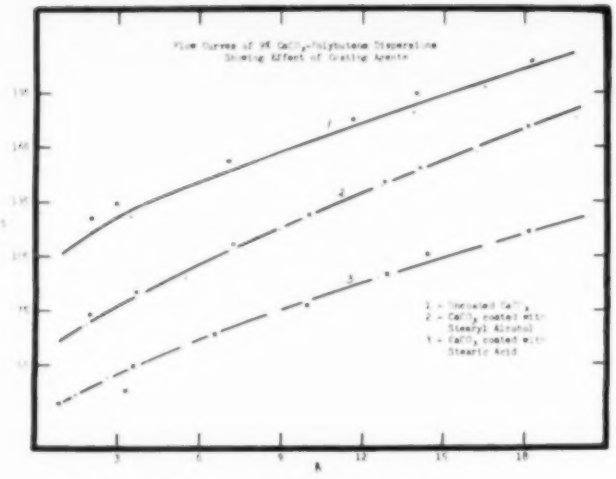

Figure 4. Flow curves show effect of coating agents.

curves for the Triol dispersions were curved throughout the entire range studied.

A significant finding of this use of the Squibb viscometer is that the full impact of particle-vehicle interactions was not realized until precision measurements in the low shear range were made. For example, in the coating of calcium carbonate by stearyl alcohol (reversible) and stearic acid (irreversible), no appreciable changes in the shape of the flow curves were observed when the rheological data were taken on a conventional rotational viscometer designed for a shear range up to 600 sec^{-1}. When the Squibb viscometer was employed, the three curves became distinct and showed evidences of structure in the low shear range; examples of this spread are shown in Figure 4. The amount of thixotropy was reduced by the coatings (6).

In contrast with numerous difficulties encountered with the aqueous systems, the flow curves obtained with the calcium carbonate systems exhibited no hysteresis (6) and were of sufficient precision to permit conclusions to be drawn concerning the effects of the polarity of the medium and of surface coatings on the carbonate; they will be published at a later date.

Although it was designed for fluid dispersions, the Squibb viscometer has proved to be versatile enough to be used in obtaining flow curves of printing inks whose viscosities range to over 200 poises. The practical limit to the consistency of materials which can be investigated in the viscometer probably lies in the order of 1000 poises. The range may be extended somewhat through the use of a more rugged torsion assembly than is currently in use, but the difficulty of embedding a slender ring in a trough-shaped cup eventually should prove too great for convenient operation.

By way of contrast, the lower limit of viscosity has not been reached, although the use of more slender filaments in the determination of stresses appears to be necessary. Development along these lines is currently in progress.

SUMMARY

A rotational viscometer has been designed and constructed for the determination of the viscosities of fluid dispersions at known rates of shear. Features of the viscometer which have been instrumental in providing high sensitivity at low shear rates are:

1. A completely frictionless suspension.
2. Application of shearing stress to both sides of the torsion member.
3. Absence of end effects, or at least their relegation to second order effects.
4. Large torque arm coincident with the requirement of only a few milliliters of sample.
5. Heat transfer to and from the thermostatic medium through both walls of the cup.
6. A torque measuring assembly of low inertia.
7. Minimization of effect of eccentricity in the cup-ring assembly by the use of a null point method for determining deflections.
8. A closer approximation to the ideal parallel plate viscometer than is customary with rotational viscometers.

A critical evaluation of the performance of the instrument has been made, and an insight has been given into the types of problems which can be studied by means of the viscometer. Results of studies on printing inks, pharmaceuticals, and other dispersions will be presented in the near future.

ACKNOWLEDGMENTS

The authors are indebted to the Squibb Institute for Medical Research for support of this work, for the encouragement given by Elliot Bartner, and for the supervision of the construction by Edward Grubel of that organization. Lehigh University personnel who participated in the evaluation of the viscometer were J. C. Miller, G. J. C. Frohnsdorff, M. N. Hoechstetter and R. C. Budias, whose contributions are gratefully acknowledged.

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Effect of Fillers . . .

(Continued from page 23)

however, large differences are indicated between different fillers.

No attempt will be made to quantitatively explain the damping results. It is interesting to note, however, that it is possible for the heat produced (proportional to $G\delta$) to be greater in a filled material than in the unfilled polystyrene while at the same time the damping δ is less for the filled than for the unfilled material. Zenzer (2) has shown that slippage between grain boundaries in metals can give rise to energy dissipation. The excess of energy dissipation in the filled materials over that of unfilled polystyrene might be due to a slippage of the filler particles in the plastic matrix.

SUMMARY

The dynamic shear modulus and mechanical damping were measured on polystyrene containing various

(Please turn to page 61)

Time and Temperature Effects on the Deformation of Asphaltic Bitumens and Bitumen-Mineral Mixtures

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ABSTRACT

The development of a nomograph which allows easy determination of the stiffness of bitumens as a function of time and temperature is briefly described. This system requires the knowledge of two routine test data of the bitumen, one to indicate its hardness (ring and ball softening temperature), the other to indicate its rheological type (penetration index).

A further study has revealed that for all deformations the deformation (stiffness) of bitumen-aggregate mixtures of low-voids content is a function of only the stiffness of the bitumen it contains (this stiffness being taken at the appropriate time and temperature) and the volume concentration of the aggregate. As a consequence, a simple system can be built up by means of which the deformation of all bitumens and bitumen-mineral mixtures can be quickly derived with an accuracy sufficient for engineering purposes.

INTRODUCTION

Bitumen, either alone or mixed with mineral aggregate, is used as a roadmaking or a building material, and the engineer consequently is interested in the deformations and stresses occurring under the prevailing conditions. Being a visco-elastic material, however, its mechanical behavior depends on time of loading (frequency) and temperature. Moreover, the great number of bitumen grades on the market differ considerably in properties, and in most applications the bitumen is mixed with large quantities of mineral aggregate. Since all these factors influence the mechanical behavior, describing or predicting the properties of a mixture from a small number of basic data seems an impossible task.

We have found, however, that the behavior of bitumens alone can be described in a simple way by using a nomographic representation of experimental results. All essential factors can be incorporated in this nomograph and a good correlation with data from standard test methods obtained. Only the essential principles of this nomographic representation of the behavior of unmixed bitumens will be discussed in this paper; for details the reader is referred to two earlier publications.[1, 2]

Following these results it will be shown how the behavior of a bitumen aggregate mixture is related to the properties of the particular bitumen it contains and the amount of aggregate added.

DEFINITION OF STIFFNESS

For simplicity we shall consider only stresses and deformations so small that the two are proportional. This requirement is not a serious restriction, since the object in applications is generally to avoid excessive deformations and hence the nonlinear range is seldom of interest. In fact, at low temperatures and short times of loading, linear behavior is found to occur up to the point of brittle fracture. At high temperatures and long loading times most bitumens behave almost as a Newtonian fluid, and our condition is again fulfilled. There is a range of conditions, however, where some grades show distinct nonlinear effects; we shall refer to this point later.

Two ways of testing the material were used: the static creep test (constant stress experiment) and the dynamic test under a sinusoidally alternating load.

If linear behavior is postulated we may define a static $S(s)$ and a dynamic stiffness modulus $S(d)$ as follows:

$$S(s) = \sigma/\varepsilon = \text{Constant tensile stress/total strain} \quad (1)$$

$$S(d) = \bar{\sigma}/\bar{\varepsilon} = \text{amplitude of alternating stress/amplitude of strain} \quad (2)$$

In general $S(s)$ is a function of temperature and time of loading; likewise, $S(d)$ depends on temperature and frequency.

If we calculate $S(s)$ for a Maxwell body we find:

$$S(s) = E_i / (1 + t/\theta_i) \quad (3)$$

where E_i is the Young's modulus of the Hookean spring and

$$\theta_i = \lambda_i / E_i \quad (4)$$

is the relaxation time; λ = Trouton's coefficient of viscous traction = 3 × normal viscosity coefficient in shear for an incompressible material, and t is the time the constant stress is operative.

For the dynamic stiffness we find

$$S(d) = E_i (1 + 1 / \omega^2 \theta_i^2)^{1/2} \quad (5)$$

where ω is the angular frequency.

It is easily seen that both for t and $1/\omega << \theta_i$ and for t and $1/\omega >> \theta_i$ equations (3) and (5) yield the same results if $1/\omega$ is substituted for t or vice versa. Indeed Figure 1 shows that there is only a small range where $S(s)$ and $S(d)$ do differ appreciably if the same scales are used for plotting 1 and $1/\omega$.

If the same procedure is used for a model consisting of a Voigt element and a Hookean spring in series, results are as depicted in Figure 2. Here too there is not a great difference between the two curves from a practical point of view. In fact, the maximum dis-

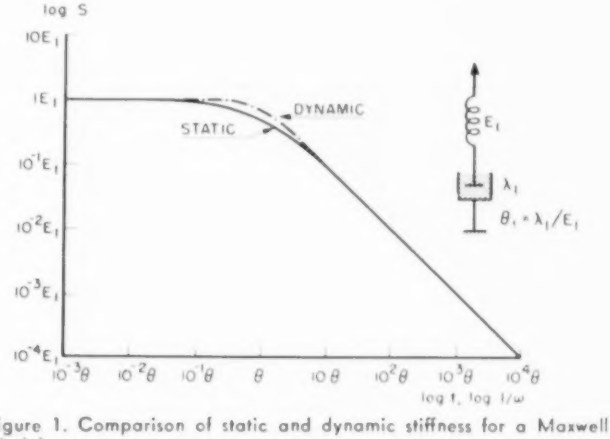


Figure 1. Comparison of static and dynamic stiffness for a Maxwell Model.

crepancy in both figures amounts to 40 percent. This may be a large difference when we are interested in relaxation spectra and correlation to molecular structure. From a practical point of view, however, the discrepancy is small compared to the enormous variation of many decades in the logarithm of the stiffness with temperature and frequency. No further distinction will be made, therefore, between S *(s)* and S *(d)* except as expressly stated.

As the behavior of any visco-elastic material which shows linearity can be represented by a model of an infinite number of Voigt elements in series,[1] S*(s)* and S*(d)* can generally be considered identical for practical purposes.

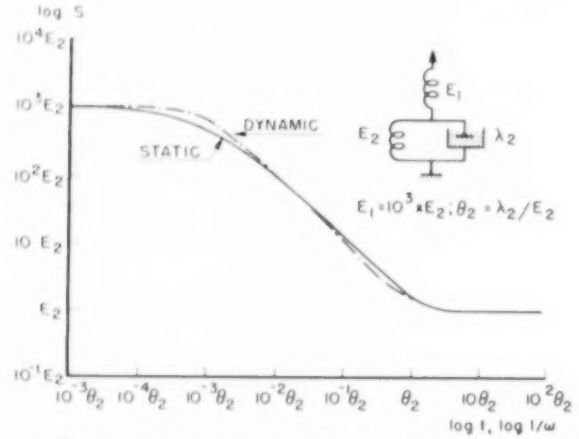


Fig. 2—Comparison of static and dynamic stiffness for a three-element model.

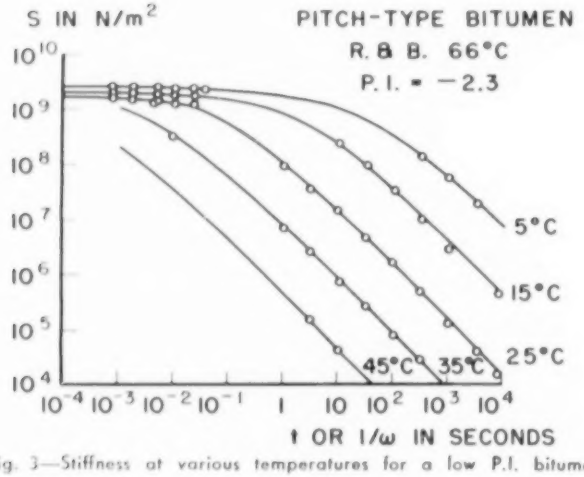


Fig. 3—Stiffness at various temperatures for a low P.I. bitumen.

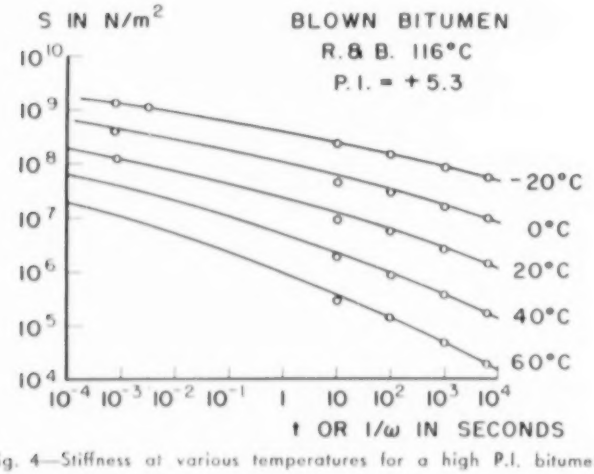


Fig. 4—Stiffness at various temperatures for a high P.I. bitumen.

From the foregoing, it is clear that for a perfectly elastic behavior, the stiffness S is independent of time or frequency and for a purely viscous behavior S is inversely proportional to time of loading or proportional to the frequency. In the latter case it follows from (3) and (4) that

$$S = 3\eta/t \qquad (6)$$

or

$$S = 3\eta\omega \qquad (7)$$

where η is the viscosity coefficient.

The two methods of loading cover a large field of practical applications. When a different loading procedure is used, the deformation can always be calculated from the results of either method. For, as we postulated a linear behavior, the superposition principle holds, and the deformation resulting from the stress actually applied can always be found by analyzing the stress-time curve, replacing it by a number of successive loading steps of known type, and adding the resultant deformations.

In fact, this procedure, when related to the dynamic stiffness, is identical to the well-known Fourier analysis.

EXPERIMENTAL RESULTS

Measurements were carried out in various ways. For the constant stress experiment, tensile and bending tests and rotation viscometers (Couette type) were used; for the dynamic method, flexural vibrations of free and clamped bars of the material.

As indicated, results of static and dynamic measurements are plotted in the same graph, the time of loading for the dynamic experiment being replaced by the inverse of the angular frequency ω.

There will never be any doubt as to whether the plot refers to a static or a dynamic experiment, since dynamic tests were used only for smaller times than 1 second and static tests only for longer times.

Typical results are shown in Figures 3 and 4 for two bitumens, one having a very low penetration index, the other, a blown bitumen, a rather high penetration index. Results are given for various temperatures.

Attention should be paid to the wide ranges of loading time and stiffness covered by these graphs.*

It is seen from Figure 3 that the low P.I. bitumen behaves almost purely elastically at short times of loading, to show a fairly sharp change at longer times, where the behavior rather soon becomes purely viscous (slope of 45°). The behavior of the blown bitumen is quite different, the decrease in stiffness with increasing time of loading being far smaller. This is due to retarded elastic effects being pronounced for this bitumen.[1]

The curves in Figures 1 and 2 are drawn in such a way that they coincide when shifted in the direction of the long-time axis. It is seen that in this way good agreement with the experiments can actually be obtained. This is a most important conclusion, for it greatly facilitates the description of the rheological behavior. Mathematicaly it can be expressed by saying that the de-

*In the system used here, the unit of force is the newton (N), the unit of mass is the kg and units of length and time are metre (m) and second respectively. These units differ from cgs units only by whole powers of 10. Consequently the unit for the stiffness is 1 N/m^2 = 10 dyne/cm^2 = 1.02 × 10^{-5} kg/cm^2 = 1.45 × 10^{-4} lb./sq. inch, while for the viscosity, 1 $N\ sec/m^2$ = 10 dyne sec/cm^2 = 10 poise.

pendence of the stiffness on time and temperature can for each bitumen be described by one variable, which is composed of the log of time plus some function of the temperature or:

$$f(S) = -\log t/t_a + \chi (T) \qquad (8)$$

Here t_a is some constant with a time dimension, and f and χ are empirical functions; t_a, f, and χ will differ for the two bitumens.

ROUTINE TEST METHODS REFERRED TO

Such a representation would not be of much help if it were necessary to determine the constant t_a, f, and χ for each bitumen separately. However, a fair correlation with data from routine test methods proved to be possible. These routine methods are briefly mentioned here; for further details the reader is referred to the literature.[4]

(a) Penetration. This is defined as the depth, measured in 1/10 mm, which a standardized needle penetrates into a bituminous surface under a constant load (100 g) in a specified time (5 sec). The method has been standardized by ASTM under No. D 5-25 and by IP under No. 49/46.

(b) Softening Point Ring and Ball. A ring of given dimensions, filled with bitumen, is loaded with a steel ball (3.5 g). The whole is heated in a bath, and the temperature at which the bitumen reaches a certain deformation is reported as the Ring and Ball softening point. The method has been standardized by ASTM under No. D 36-26.

(c) The Penetration Index (P.I.) This is not a test method but an index figure, introduced by Pfeiffer[4] and Van Doormaal to indicate the temperature susceptibility of the penetration of a bitumen. This requires a somewhat lengthier discussion.

If the logarithm of the penetration is plotted against the temperature (T), an almost straight line is obtained, suggesting the use of the slope of this line as a susceptibility index. This would require two penetration tests at two temperatures for each bitumen. If, however, the straight line is extrapolated to the temperature of the Ring and Ball softening point, the corresponding penetration is approximately 800. Therefore the P.I. can be calculated from one penetration and the Ring and Ball softening point, the slope of the penetration line being given by

$$\frac{\log 800 - \log \text{pen.}}{T\,(R\,\&\,B) - T}$$

where T refers to the temperature in °C at which the penetration test is carried out.

The penetration index (P.I.) as used by Pfeiffer and Van Doormaal is not directly proportional to this slope, but the relation is given by the formula:

$$\frac{\log 800 - \log \text{pen.}}{T(R\,\&\,B) - T} = \frac{20 - \text{P.I.}}{10 + \text{P.I.}} \cdot \frac{1}{50} \qquad (9)$$

This relation, which is quite arbitrary, was chosen for historical arguments, one of the reasons being that a Mexican bitumen with a penetration of 200 at 25°C gives a P.I. of zero.

As is clear from (9), a bitumen with a penetration which is completely independent of the temperature has a P.I. = +20, whereas the other extreme, a bitumen with an infinite temperature susceptibility, should have a P.I. = −10.

These extremes are never found in practice; the P.I. of bitumens varies from −2.6 to +8. The lower the P.I., the higher the temperature susceptibility.

The penetration index can easily be read from the nomograph given in Figure 5. As can be seen from that nomograph and also from equation (9), the only data required to determine the P.I. are a penetration value and the difference between the temperature at which it is determined and the temperature of the Ring and Ball test.

A close relationship exists between the P.I. and the rheological type of asphaltic bitumen. In fact this is the real importance of this index figure, as will be shown below.

NOMOGRAPHIC REPRESENTATION OF RESULTS

The experimental results of Figures 3 and 4 can be represented nomographically, as is clear from equation (8), by a line coordinate chart. Such a chart consists of three parallel lines on each of which are suitable scales for **t**, **T**, and **S**, the scale for **t** being a logarithmic one. This would not solve our problems, however, since for each different bitumen grade a different nomograph should be used.

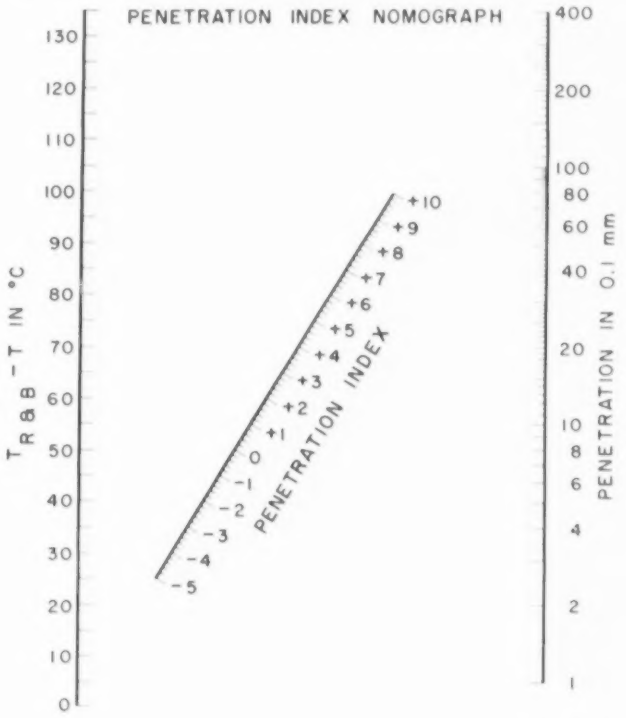
However, it was found that two bitumens of the same rheological type (same P.I.) at the same time of loading or frequency have equal stiffness at temperatures which differ from their respective Ring and Ball temperatures by the same amount. This means that equation (8) can be transformed to

$$f(S) = -\log t/t_a + c\psi\,(T\,(R\,\&\,B) - T) \qquad (10)$$

in which the temperature function χ of T alone has been replaced by a function (ψ) of $T(R\,\&\,B) - T$.

A nomographic representation based on equation (10) in which the temperature scale now refers to the difference between $T(R\,\&\,B)$ and the temperature at which we want to know the stiffness has a far greater

Fig. 5—Nomograph for determination of Penetration Index.



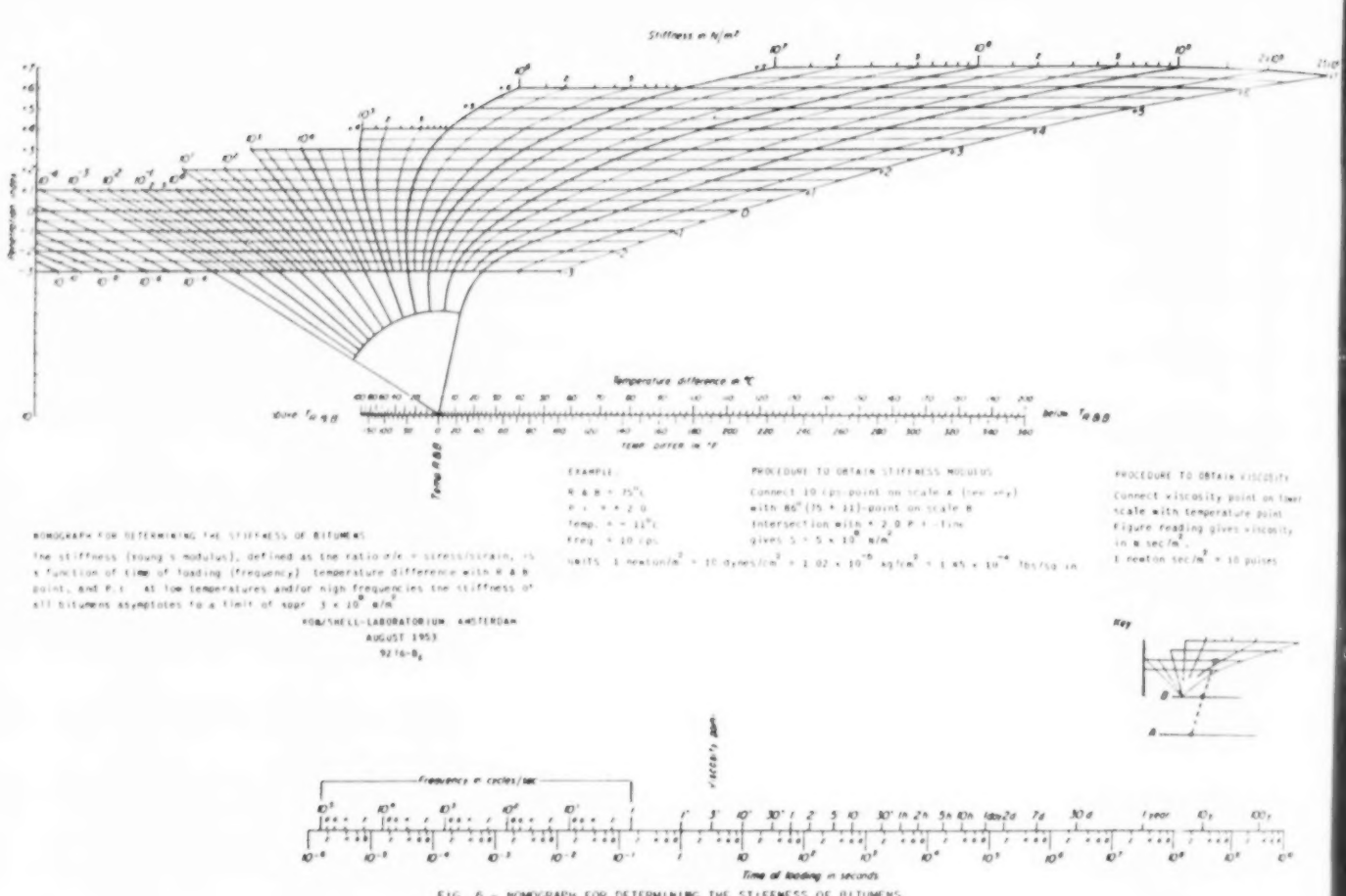


FIG. 6 – NOMOGRAPH FOR DETERMINING THE STIFFNESS OF BITUMENS

applicability, because it can be used for all bitumen grades having the same P.I. (same rheological type).

Moreover, it has been found that the same function ψ can be used for all types by giving the "constant" **c** a suitable value. In other words, by making c a function of P.I. only, equation (10) can be made to represent the stiffness of all bitumens as a function of **t**, **T***(test)*, **T** *(R & B)*, and P.I.; the f(S) also depends on P.I. This means that for all bitumens the same scales for time and temperature difference with Ring and Ball temperature can be used; the scale for S, however, lies at different distances and is graduated in different ways according to the rheological type (Penetration Index) of the bitumen.

On this basis the nomograph of Figure 6 was constructed, from which the S value can very easily be read. The use of the nomograph may be clear from the outline given in the figure.

In the following the nomograph will be used to deduce the stiffness of the bitumen in the range of $S > 10^7$ N/m² only. Figures 7 and 8 illustrate how nomograph readings in this range compare with experimental results.

In Figure 7 the stiffness at a frequency of 200 cps of a number of bitumens is plotted as a function of **T***(R & B)* — **T***(test)*; the curves in this figure are obtained from the nomograph of Figure 6. The dots and crosses on the P.I. = 2.1 line represent two different bitumens of the same penetration index which differ in R & B temperature by 53°C. The various symbols for P.I. = −2.6 indicate three different bitumens, the extremes of which differ by 20°C in R & B temperature. It is seen that points come close together and that there is a fair agreement with nomograph readings. Results of static tests for a loading time of 1000 sec are compared with nomograph readings in Figure 8. Again there is good agreement and also here the **T***(R & B)* — **T***(test)* can be shown to hold: there are experimental results of three bitumens for P.I. = −0.4, the extremes of which differ 15°C in R & B hardness.

In general, there is also good agreement between the nomographic representation and experimental results in the lower range of the stiffness. For details we refer to an earlier publication.[2]

As can readily be seen in Figure 7, at low temperatures the stiffness of all bitumens asymptotically approaches a limit of 3×10^9 N/m² (3×10^{10} dyne/cm²). At these temperatures the stiffness also becomes independent of the frequency (Figure 3), and the material

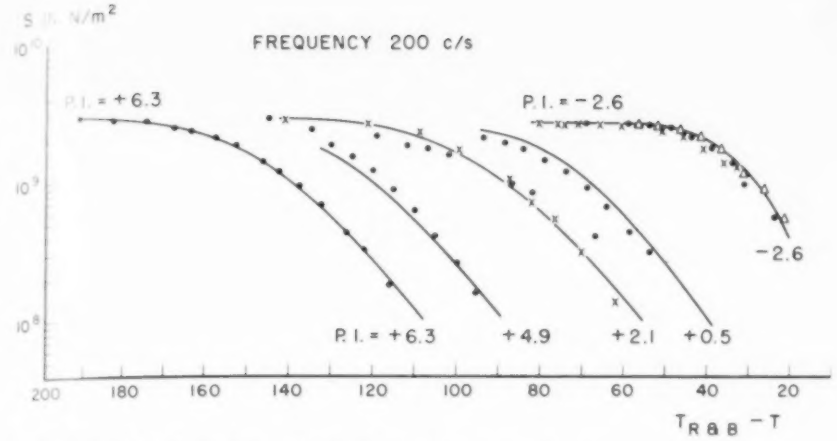


Fig. 7—Dynamic stiffness from vibrating bar experiments for various bitumens plotted against the temperature difference between R&B and test temperature.

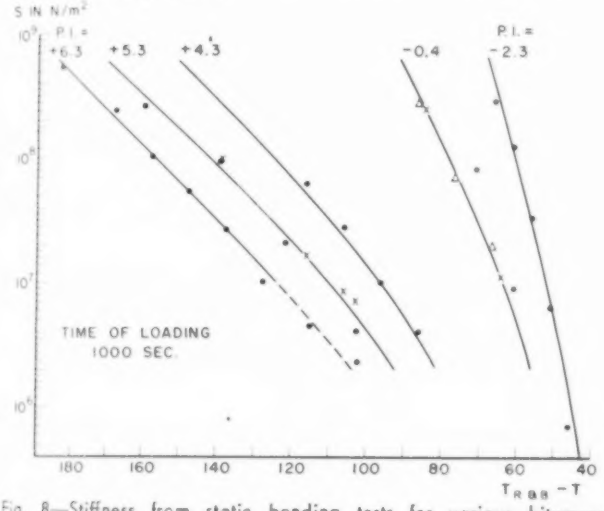


Fig. 8—Stiffness from static bending tests for various bitumens plotted against the temperature difference between R&B and test temperature.

behaves as a purely elastic one. It is also apparent from Figure 3 that the behavior of a low P.I. bitumen may change from purely elastic into purely viscous very rapidly when the frequency is decreased by a factor 10^3 or the temperature is decreased 10° to 20°C. In fact, this bitumen behaves almost like a Maxwell body, as may be seen by comparing the curves of Figure 3 with those of Figure 1.

The upper left part of the nomograph is blank and does not permit reading of the stiffness. In this range the higher P.I. bitumens show nonlinear effects, and description with a stiffness modulus becomes inadequate. Extrapolation into this region should not be made.

BEHAVIOR OF MIXTURES OF BITUMEN AND MINERAL AGGREGATE

As long as stresses and deformations are small, we can again introduce the stiffness to describe the properties of a mixture. In the results given below, which are all obtained by dynamic testing methods, the condition of linearity is fulfilled throughout.

Two experimental methods were used: flexural vibrations of a bar, and torsional vibrations of a cylinder of the material. Bars for the bending vibration test are 1 cm thick, 2 cm wide, and 10 to 25 cm long. Bars are suspended in the nodes of the vibration type chosen and set into vibration electromagnetically by attaching a small plaque of iron on one end. Opposite this iron plaque is a small electromagnet which is energized by a resistance-capacitor generator giving a current of adjustable and accurately known frequency. On the other end of the bar a similar plaque is attached opposite which is an electromagnet activated by the vibrating bar. The current in the latter electromagnet is used to register the amplitude of the vibrations. The bar is brought into resonance and from the resonance frequency, dimensions, and density, the stiffness can be calculated. By having each bar vibrating both in the basic frequency and in a few overtones and by using bars of various length, the stiffness can be determined at a number of frequencies between 50 and 2000 cps. This method was used for bitumens alone (results given by Figure 7 were obtained in this way) and for mixtures containing not too coarse mineral matter. Moreover it is suitable in the high stiffness region only, since in the lower region damping is usually too high to permit accurate determination.

For the torsional vibration test, cylindrical rods 4 to 8 cm in diameter and 10 to 30 cm long are used. Samples of this size can be manufactured in molds under high pressure, simulating conditions of compaction used in road-building. Also, the dimensions permit the use of coarse particles (up to 1 cm). The cylindrical sample is fixed tight at its lower end to an infinite mass (the laboratory building), and to its top a cylindrical mass many times larger than that of the sample is attached. Torsional vibrations are set up in this system around its axis by means of eccentric weights attached to the shaft of an electric motor which is fixed to the upper mass. From the resonance frequency of the system the stiffness in shear

$$S(G) = \tau/\gamma = \frac{\text{amplitude of shear stress}}{\text{amplitude of shear strain}}$$

can be calculated. By using masses of varying moment of inertia the stiffness may be obtained at various frequencies ranging from 10 to 150 cps. Owing to the mechanical way of excitation this method can be used in a lower region of stiffness, where damping is usually higher and more energy is required to sustain vibration.

Experimental results obtained by adding various amounts of mineral matter to the same bitumen are shown in Figure 9 as a function of temperature. It is seen that the stiffness of the mixture increases regularly with increasing concentration of the mineral $[\mathbf{C}(v)]$ and this effect is slightly greater at higher temperatures, where the bitumen is softer.

Figure 10 is an example of results obtained when measurements are carried out with bitumens, but at temperatures such that the bitumen component shows the same stiffness. For the two bitumens in this figure the stiffness of the bitumen alone is 2×10^9 N/m², which is only slightly less than the maximum value. The stiffness of the mixture has been plotted as a function of the volume concentration of the mineral aggregate $[\mathbf{C}(v)]$.

In spite of the fact that a great variation in particle size and grading of the minerals (fillers, sand, and mixtures of the two) has been used, all points lie practically on one curve. Evidently, these factors do not affect the

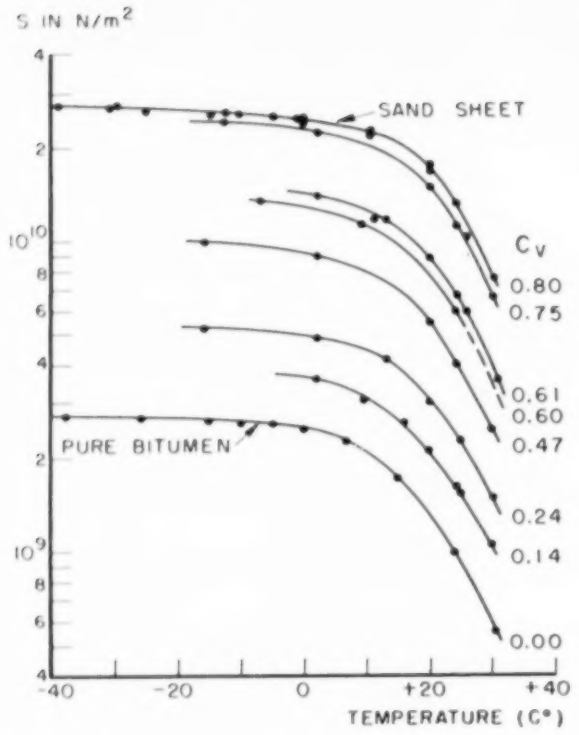


Fig. 9—Dynamic stiffness of mixtures containing the same bitumen and various amounts of aggregate (sand and/or filler) as a function of temperature. Properties of the bitumen: R&B 54°C, pen. 13, P.I. -2.6.

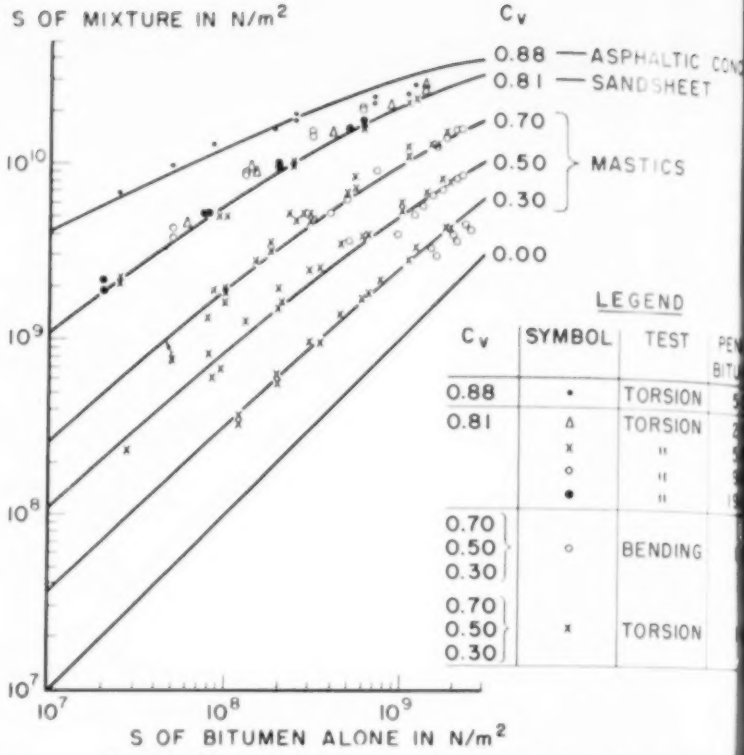


Fig. 11—Effect of concentration of mineral aggregate on stiffness of mixtures. Symbols either refer to different bitumen grades or to different experimental methods.

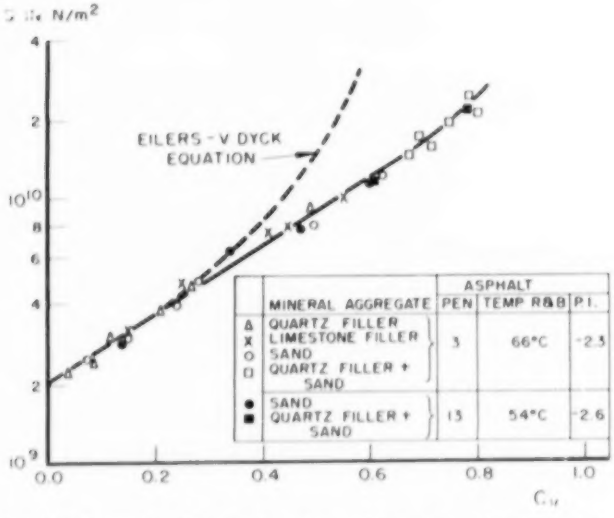


Fig. 10—Dynamic stiffness of various mixtures as a function of the volume concentration of mineral aggregate (Cv). Temperature and frequency conditions are such that the pure bitumen contained in the mixtures has a stiffness of 2×10^7 N/m².

results, so that it is only the volume concentration of the mineral that counts. Similar curves can be drawn when other values are chosen for the stiffness of the bitumen.

Results shown in these last two figures led to the hypothesis that the stiffness of a mixture is governed by two factors only: (a) the volume concentration of the mineral aggregate [$C(v)$] and (b) the stiffness of the bitumen alone.

To check this hypothesis further, additional experiments were carried out in the lower stiffness region with a variety of mixtures. Contrary to the results shown in Figures 9 and 10 most of these experiments were carried out with the torsional vibration test. Apart from a number of mastics (up to 70 vol percent mineral) sandsheet and asphaltic concrete mixtures were also measured; they differ from the mastics in that they cannot be poured at high temperatures, but have to be compacted in molds. This compacting requires some time, during which the mixture must be kept at a high temperature, resulting in a hardening of the bitumen owing to oxidization. To allow for this hardening the samples after being tested were put in carbon disulphide in which the bitumen readily dissolves. The bitumen was then carefully recovered from the solution and its R & B and P.I. determined. The R & B generally showed a rise of a few degrees centigrade as compared to the original bitumen, the P.I. increased only by a few tenths depending on the treatment of the sample. No difference in properties was found between original bitumen and bitumen recovered from mastics.

With the help of these data the stiffness of the bitumen as contained in the mixture can easily be read from the nomograph of Figure 6. When now we plot the stiffness of the mixture as measured against that of the recovered bitumen as determined from the nomograph at the appropriate temperature and frequency, a single curve for all types of bitumen results if the same volume concentration of mineral is used.

An illustration of this general result is shown in Figure 11. In order to enable an easy comparison between results obtained with the two experimental methods the stiffness in shear obtained from the torsional vibration test is multiplied by a factor three to obtain the (normal) stiffness in tension. This is allowed when the material can be considered as incompressible, which is true in the lower stiffness region. If the stiffness of the bitumen approaches the upper limit of 3×10^8 N/m² closely, neither the bitumen alone nor the mixture is incompressible. Then Poisson's constant may be as high

as 3 for the pure bitumen[1] and 4 for the mixture, which requires multiplication by a factor 2.7 to 2.5 in that region. In general, however, the correlation at which we aim is not that accurate, and for the sake of simplicity a factor of three is used throughout.

If we now look at Figure 11 more closely, we first see that there is a reasonable agreement between the results obtained with the two experimental methods used for measuring the mastics. Furthermore it is seen that there is no systematic difference between results obtained with the various bitumen grades used in the sandsheet mixtures though they do differ significantly in hardness. Also there is a systematic increase of the stiffness with increasing volume concentration of mineral, as was shown in more detail in the foregoing figure.

Many more results obtained by the author confirm the general trend shown in these few examples. A number of other types of bitumens were used, most of them being of the conventional distilled type like those for which results are shown in Figure 11; but three blown grades were also employed with penetration indexes up to +6.0, one of them being a blended grade. Results from all these tests agreed closely with the results shown in the figure given here.

As to the minerals used in the mixtures, all contained both sand and fillers except the asphaltic concrete mixture which contained 50 parts by weight of chippings to 100 parts of mineral. The amount of fillers used range from 30 p.b.w. [$C(v) = 30\%$] to 7 p.b.w. [$C(v) = 88\%$].

The fillers used were limestone or quartz, both having particle sizes largely between 2 and 70 microns, the amount of particles below 2 microns being 7.7 percent and 7.0 percent of total filler for limestone and quartz, respectively.

The quartz filler was rather coarse and contained 28 percent particles larger than 50 microns, the limestone filler only 2 percent. Clays, carbon black, and other fine particles were not used.

All these results show that the stiffness of a bitumen-mineral mixture is determined only by the stiffness of the bitumen alone and the volume concentration of the mineral aggregate.

This statement applies to the conditions investigated, i.e., to linear mixtures with bitumens having a voids content below 4 percent.

The influence of the type of bitumen, the temperature, and the frequency are all accounted for by the stiffness of the binder.

It is clear that this is an important practical result, for with the help of Figure 11 and the nomograph of Figure 6 it is now possible to deduce immediately the stiffness of a certain mixture under any condition if only three material data are given: R & B and P.I. of the bitumen, and $C(v)$ of the aggregate in the mixture.

The curves in Figure 11 have a clear tendency to converge on the point where both S mixture and S bitumen are 7×10^{10} N/m^2. This value therefore represents the stiffness (elastic modulus) of a solid mineral sample. It is clear that if the bitumen could have the same stiffness, the stiffness of the mixture should be independent of $C(v)$.

Now the bitumen cannot have a stiffness beyond 3×10^9 N/m^2 but this is high enough to deduce the effect of adding large amounts of aggregate, as compared to the effect when the bitumen is softer. For example the asphaltic concrete mixture has a stiffness which is about 400 times that of the bitumen at 10^7 N/m^2 and only 30 times at 10^9 N/m^2.

It may now also be clear that formulas found in literature for the effect of adding mineral matter to viscous fluids do not apply for our conditions, since it is always assumed that the dispersed material is infinitely hard as compared to the fluid. Moreover most formulas are only valid for very small concentrations.

For concentrations up to 30 percent, however, it was found that our results can be represented quite well by an empirical equation of Eilers[5] and van Dijck reading:

$$S\ mix\ /\ S\ bit = [1 + \{1.25\ C(v)\ /\ (1 - 1.28\ C(v))\}]^2$$

This equation was made to obey Einstein's[6] well-known formula for very small concentrations and to give an infinite value for the ratio when $C(v) = 0.74$, which is the volume concentration of the densest packing of spheres of equal diameter.

How this formula applies to our data for a high bitumen stiffness is shown in Figure 10. It is seen that up to $C(v) = 0.30$ there is a very good agreement. Also from Figure 11 it is seen that the line for $C(v) = 0.30$ is straight and has a slope of practically 45°, which means that the formula also holds for lower values of the stiffness.

If however the concentration is larger than 30 percent, the effect of adding mineral cannot be accounted for by simply multiplying the stiffness of the bitumen by a factor depending on $C(v)$ only, but the results also depend on the stiffness of the bitumen as indicated above.

ACKNOWLEDGMENTS

The author is indebted to Dr. R. N. J. Saal and Messrs. F. Dijkstra and W. Heukelom for their interest and assistance and to the management of the Royal Dutch Shell Group for their permission to publish this work.

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Viscous Heat Effects . . .

(Continued from page 40)

β_1 = constant in parabolic law temperature dependence of viscosity § = 2.05×10^{-2}

β_2 = constant in parabolic law temperature dependence of viscosity § = 1.14×10^{-4}

*Estimated

**Rough estimate

†Calculated from the $D_c(\tau_R)$ line on figure 4

§Calculated from Tordella's flow curves at 150°, 190°, and 250°C.

In Table 4 are given the results of the calculations based on the above assumed values of the physical properties. These results include the maximum temperature attained in the extrusion process, the average exit tem-

Viscous Heat Effects . . .

(Continued from page 53)

perature of the fluid, and the deviation in the flow curves resulting from viscous heat effects. The experimental setup would be expected to give results lying somewhere between the isothermal wall and the insulated wall results, but probably closer to the latter.

It is unfortunate that there are no data available on the average exit temperature to compare with these calculations. Above $\tau_R = 5 \times 10^5$ the theory predicts temperature effects sufficiently large that some physical properties of the fluid might be affected. A comparison is given between the theoretical and experimental D/D$_o$ values; it is seen that the calculated values are generally somewhat too small. It is difficult to draw any conclusions concerning the discrepancy, in view of the doubtful values of the physical constants used in the calculations. Nevertheless the theory does seem to predict the value of τ_R above which viscous heat effects become important. Furthermore the general shape of the predicted flow curve is correct.

TABLE 4

Tabulation of Calculated Results for the Viscous Heat Effects in the Extrusion of an "Alathon" 1 Polyethylene Resin.

			Isothermal Wall			Insulated Wall			
τ_R (dyn cm^{-2})	γ_R (sec^{-1}) 7x10^{5}	τ_R	Tav (°C)	Tmax (°C)	D D$_o$	Tav (°C)	Tmax (°C)	D D$_o$	D D$_o$ Expt'l
$2 \cdot 10^5$	1.75		0	0	1.0	0	0	1.0	1.0
$5 \cdot 10^5$	0.28		0	0	1.0	1	2	1.0	1.0
10^6	0.070		1	2	1.0	4	7	1.1	1.2
$2 \cdot 10^6$	0.0175		4	7	1.1	12	24	1.4	1.8
$5 \cdot 10^6$	0.0028		20	52	1.6	37	130	2.6	4.3
10^7	0.0007		69	240	3.9	94	490	6.7	10.4

ACKNOWLEDGMENTS

The author wishes to thank the staff of the Polychemicals Department of the Du Pont Experimental Station for suggesting this problem and for making their facilities available to him. Particular thanks are due Drs. W. H. Linton and J. P. Tordella with whom the author had many interesting and profitable discussions. During the latter part of the work on this problem, financial support has been provided by the National Science Foundation. In addition, the author desires to thank Prof. Charles F. Curtiss (Chemistry Department, University of Wisconsin) who read the manuscript and suggested some changes, and Mr. T. H. Chin (Electrical Engineering Department) who assisted with some of the calculations.

Dynamic Creep of Plastics . . .

(Continued from page 21)

ACKNOWLEDGMENTS

The results reported in this paper represent a part of a Master of Science Thesis by W. J. Schuman conducted under the supervision of Joseph Marin. The authors appreciate the technical help of Dr. L. W. Hu and the financial assistance of the Research Corporation in conducting this research.

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GOLDEN GATE

R. G. Newhall

The May meeting of the Golden Gate Chapter under the leadership of president John L. Morrill featured a lively discussion of the potential for large area vacuum forming and an elaboration of some of the unusual problems involved. Program chairman Roy Theiss introduced the speaker, George Parker of Dimensional Displays, Inc., who showed a series of slides illustrating some of the large vacuum formed signs produced by his company for its affiliate, Shopping Center Promotions. These signs, internally illuminated displays some 9' high, were mounted on lamp posts as part of an overall decorative scheme for a shopping center.

George Parker

These displays are vacuum formed from Eastman's Tenite butyrate extruded by Jet Specialties Company in sheets 0.080" thick, 54" wide and 96" long. Dimensional Displays uses a double table vacuum forming machine of its own design. The sheets are warmed by Chromalux strip heaters 66" long each having an input of 3,000 watts. These strip heaters, arranged in banks of 35, give an average power density of 10-12 watts/sq.in. The total power input is accurately controlled by a percentage timer. The present machine is somewhat limited by the size of its vacuum tank—80 gallons—and the speaker pointed out that tank size was generally more important than pump capacity in large area vacuum forming.

THE *Injection* MOLDERSCOPE

By A. R. Morse

President, Injection Molder's Supply Co.

"It is the freedom to disagree, to quarrel with authority, in intellectual matters to think otherwise, that has made this nation what it is today." Dr. James B. Conant

Technical Notes On Nylon Molding

SHUT-OFF NOZZLES

Shut-off type nozzles should NOT be used for general nylon molding work because they may cause dangerous blow backs thru the feed opening, if the material in the cylinder is accidentally overheated or heated for prolonged periods. Only in very rare cases where excessively high front cylinder temperatures must be used (as in molding some very thin wall sections) can shut-off type nozzles possibly be suggested. Even then the danger of blow backs would still recommend consideration of the standard Du Pont design.

With shut-off nozzles no way exists of checking the condition of the melt in the cylinder. The nozzle should be open between shots to permit observation of the material or quick purging when necessary, or to allow the cylinder to relieve itself if overheated without a dangerous blow back.

Nozzles with spreaders or torpedoes have not proven satisfactory because the molding range is much too narrow. Nozzles with reverse tapers of less than 1" are generally not satisfactory.

THE DU PONT DESIGN

The Du Pont design nozzle features a 1¼" long reverse taper, and heated extension so the restricted throat of the nozzle is directly under a special extra high wattage heater band. The design is based on exhaustive tests of dozens of types of nozzles of every conceivable type by Du Pont technicians. Furthermore, IMS has built and tested over 10 different types proposed for manufacture by various persons or concerns, but none was without some vital objections for general use.

The final Du Pont design represents the simplest, cheapest, safest, best, most foolproof and universal nylon nozzle conceived to date. Its successful use under the widest variety of molding conditions, completely bears out its practicability. Any "novelty" nylon nozzle should be considered only after re-checking EVERY detail of your overall molding operation such as thermocouple condition and location for nylon; heater bands and their arrangement, condition, and wattage; condition of pressure gages; line voltages; pump condition; booster relays and timers and all process factors that could affect the molding operation. If the nylon is adequately dry and the temperature of the cylinder and nozzle is properly distributed and controlled, the standard reverse taper design can be used to mold practically any job with practically any heater.

Adaption To Different Cylinders

Due to the added 1¾" heated extension necessary to incorporate the 1¼" internal reverse taper, nylon nozzles are perforce longer than the standard nozzles. Two methods can be used to adapt them to standard equipment. First, an alteration can be made to the mold to recess the sprue bushing, provided the die can support the 2½" to 2¾" clearance hole required. Or second, a bolster plate can be inserted back of the stationary half of the mold to permit the extension nozzle to be used. Shortening and reducing the sprue is recommended as faster cycles are often possible with modern sprues that require less plastic to fill them.

Stock Nozzles

Certain machines have enough nozzle space on them to permit use of standard stock nylon nozzles. These are notably the Reed-Prentice with Reed or IMS Heating Cylinders; the Lewis; the Stokes; and the IMPCO. Lester, HPM, Watson and most other types require special length nozzles designed specifically to fit certain molds.

Drooling and Freezing

Excessive drooling or stringing of the nylon from the nozzle is an indication that something is wrong with the handling of the material prior to molding, or that improper heating and temperature control are being used. Material should be completely dry, and never exposed to atmospheric conditions for long periods prior to molding.

Relocation of thermocouples can often correct drooling, as can selective re-arrangement or re-grouping of the cylinder heater bands. Use of new heavier wattage bands in the rear zone, and reduction of wattage in front zone permit most any standard heater to be successfully adapted to nylon molding with the nylon design nozzle.

Incorrect or misjudged cylinder ratings in terms of nylon can also cause excessive drooling. A shut-off type nozzle will NOT correct for these conditions, and by permitting SOME molding to be done under improper conditions can result in the production of under-strength or dimensionally unstable nylon parts.

While in general nylon can be molded on any press, the cylinder should be re-rated in nylon, and a study of the heating zones made to assure that too much heat is not being pushed into the nylon in the front zone.

This is almost unavoidable with certain types of standard cylinders where the chief contact or support area of the spreader is at the very front of the cylinder; and re-arrangement of bands, couples and wattages is essential here to re-adjust the heat input to the slow melting properties of nylon. If impractical, or if drooling persists, move the job to a (preferably) larger or different make of press.

(This topic will be continued in the October issue.)

Cleveland

R. M. Hershey

Mr. Ernest P. Moslo, President of the Moslo Machinery Company, addressed the May meeting of the Cleveland-Akron Section on "Runnerless Injection Molding and Some Problems of the Injection Machine Builder." Approximately 50 members attended the meeting which was held May 9 at the Spanish Tavern in Brecksville, Ohio.

Mr. Moslo discussed problems incident to building the perfect injection molding machine and some of the reasons why they cannot get the dream machine out of stock. Mr. Moslo's talk was published in the April 1955 issue of the SPE Journal. The movie shorts, "Play Better Golf," "Fundamental and Advanced Golf," and "Saving Strokes with Snead" were shown following the meeting.

The annual golf outing was held on June 10 at Columbia Hills Country Club. A film of the Cleveland Browns Championship game was shown after dinner.

The first meeting of the fall will be held on September 12 at the Spanish Tavern in Brecksville, Ohio. The October 10 meeting will feature the semi-annual Guest Night and the annual presentation of the annual Thomas E. Orr award to students of Case Tech and Akron University.

SECTION NEWS

Edited by

Stanley Bindman

Jamison Plastic Corp.
1255 Newbridge Road
North Bellmore, L. I., N. Y.

Western New England

George Snyder

Frank Reinhart, national president, (SPE) made the wind-up meeting of the current business year a special event for the 85 members of the Western New England Section at Bradley Field's Terrace Dining Room on May 4.

Mr. Reinhart outlined what he called his "three-point program" for developing the necessary philosophical goals to make SPE a dominant force behind the growth of plastics technology. Using the initials of "S-P-E" as the inspiration for his program, he listed the three areas as (1) Service to industry and humanity through plastics engineering, (2) advancement of Profession through the elevation of the professional status and ideals of plastics engineers, and (3) Education of youth in plastics technology through recruitment and guidance programs at the college training level.

Lending added significance to Mr. Reinhart's message was George W. Martin's (section prexy) announcement of newly elected directors for the Western New England Section for the next term. The new directors are: Arthur W. Logozzo, (present program chairman) Nutmeg Chrome Corp., president; Lewis H. Gustafson (present treasurer) Pro-phy-lac-tic Brush Co., vice president; Edward J. Sweeney (present credentials chairman) Rogers Corp., treasurer; Raymond A. Mazur, American Cyanamid, re-elected secretary. Other section directors elected to head special committees were: Richard J. Plichta, Fuller Brush Co.; Alvino Aliberti, A. & M. Tool and Die Co., Inc.; and Paul Stahlberg, Monsanto Chemical Company.

The first technical speaker of the evening's program was Gordon Seiter, manager of the High Vacuum Metallizing Dept., F. J. Stokes Machine Co., Philadelphia. Mr. Seiter gave a highly informative talk on vacuum metallizing plastic parts. He confined his discussion primarily to the advantages of vacuum metallizing as a production process, and outlined some of the latest advances made in this field. One of the more enlightening sections of his presentation, which was profusely illustrated with visual aid slides, pertained to the vaporization of aluminum strips by means of electrically-activated tungsten elements in the vacuum chamber. He also fully evaluated some of the latest types of vacuum equipment now being used, which generated a high degree of interest in the group, both during his talk and in the discussion period that followed.

Second speaker of the meeting was Dr. Harold Narcus of the Electrochemical Company, Worcester, Mass., who spoke on "Industrial Applications for Metallized Conductors." Dr. Narcus exhibited a number of metallized and electroplated industrial items, with special emphasis on the electronics field. Two of the industrial areas Dr. Narcus explored for the group were printed circuitry and the use of conductive paints for plating work.

Clyde E. Morrell, Pro-phy-lac-tic Brush Co., served as technical chairman for the meeting.

Frank Reinhart being greeted at the airport at Western New England.

Central Indiana

C. R. Smith

A total of fifty guests and members attended Central Indiana Section's May meeting at the Warren Hotel in Indianapolis. Speaker for the evening was Mr. A. R. Morse of Injection Molders Supply Co., Cleveland, Ohio.

Mr. Morse's interesting talk pointed up the lack of standardization within companies and the industry as a whole on replacement parts for injection molding equipment. While acknowledging the necessity of some variables, he illustrated with picture slides efforts of his company to reduce the molders required inventory of spare parts by making them interchangeable through the use of adaptors or multiple units.

At the conclusion of Mr. Morse's talk, Section President Gene Quear of Delco Remy Div. of G.M.C., Anderson, Indiana, outlined briefly to the group, progress being made by a joint committee of General Motors and S.P.I., in setting up certification standards for injection molding machinery. To date tentative approval has been obtained from major machine manufacturers for certification of the following:

1. Clamping capacity.
2. Method of rating single shot and plasticizing capacity.
3. Machine nozzle and locating hole.

Southern

C. Veazey LeCraw

The Southern Section met June 20th at Mammy's Shanty Restaurant in Atlanta. In behalf of the membership, President Diamond presented a plaque to Mr. Raymond W. Kruse and to Mr. Jules W. Lindau, III, first President and first National Council Representative respectively, in appreciation of the fine work they did in forming the Southern Section and in getting the organization rolling during its first year. The President summarized a report given to him by our National Council Representative on the success of the June 17th Council Meeting in Minneapolis, Minn.: 1. the Society proposes to publish engineering textbooks on plastics.

Mr. Diamond then introduced his guest and main speaker for the evening, Mr. Norm Huber of the Market Development Department of the Koppers Company. Mr. Huber gave an exceptionally fine talk on Koppers' expandable polystyrene. There were many examples of molded and extruded expandable styrene, plus many excellent slides to support the talk. After a long question and answer period, the members gave Mr. Huber a long round of applause for his fine talk.

New York

J. T. Dugan

The New York Section, SPE, attempted to answer the question of "Which Plastic Material to Choose—Thermoplastic or Thermoset?" at their May 18 panel meeting at the Gotham Hotel, New York, N. Y. Approximately 100 members and guests attended the meeting which saw T. Earl Dudney, Eastman Chemical Products, Inc., and E. Y. Wolford, Koppers Co., Inc., representing the thermoplastic materials; Edward F. Borro, Durez Plastics & Chemicals, and J. Moylan, Barrett Division, Allied Chemical & Dye Corp., as protagonists for thermosetting plastics. Guy A. Martinelli, Sylvan Plastics, Inc., served as moderator.

According to Mr. Borro, the decision must be reached in the first stages of engineering. The choice, too, must be made from among many materials within a category; for example, there are over 100 active phenolic materials with 18,600 different combinations possible. As a general rule, thermosetting materials are applicable where electrical properties, dielectric strength, dimensional stability are required. Corrosion resistance and low price are two other prime factors.

The second speaker, Mr. Dudney, cited as factors influencing material choice: service requirements, part design, material limitations, and economics. He described a situation where thermoplastic material is now being recommended over thermosetting; butyrate phones in color. Another reported advantage of the new phone is that it has an average injection cycle of under one minute.

Mr. Moylan felt the controversy as applied to his company's products involved urea and styrene in decorative housings. Urea, in his estimate, showed greater resistance to scratching and crazing, was self-extinguishing, did not collect electrostatic dust, and was resistant to cold flow.

A combination of properties should determine the final choice in Mr. Wolford's opinion. He distributed property charts on 15 plastics manufactured by Koppers as a handy reference tool. Low pressure-modified polyethylene will have a tremendous impact on the industry, he felt, and competing materials will be forced to undergo modifications and improvements. This represents the cycle of choice; a different material is produced and gains favor for a time until the old is modified. The important factor in competition should be that it upgrades plastics.

Table favors were supplied by Plastic Molding Powders, Inc., and door prizes compliments of Monsanto Chemical Co. and the Section.

A total of 27 new members was added by the New York Section during the past few months, as follows: George Mayurnik, Sales Manager, Aries Laboratories, Inc.; Peter D. Monti, Manager of Extrusion Dept., Mastercrafts Plastics Co., Inc.; Martin A. Olsen, Vice-President & Sales Manager, Olsen Mark Corp.; Charles S. Grey, President, Cee-Jay Extruders, Inc.; Edmund Greacen, Manager of Model Shop, Lippincott & Margulies Inc., Robert C. Magee, Works Manager, Thomson Industries Inc.; Walter E. Rayher, Sales Engineer, F. J. Stokes Machine Co.; Melvin Rosensaft, Production Manager, Ideal Toy Corp.; Nathan S. Moss, Productions Manager, Stafford Mfg. Co., Inc.; Donald M. Joseph, Sales Manager, Plastics Division, Ciba Company, Inc.; Dr. Ulrich Heubaum, Vice-President, Metachem, Inc.; Raymond S. Portner, Sales Rep., Ball & Jewel, Inc.; Frank C. Ludwig, Vice-President, Deoxygenation Corp.; Walter E. Morris, General Manager, Vizofilm Mfg. Corp.; Leroy A. Darling, Chemistry and Process Engineer, Sperry Gyroscope Co.; Lee Bodin, Plant Manager, Plastic Jewel Co.; J. Edward Brister, Dept. Head, Molding & Extrusion Div., Bakelite Co.; Joseph T. Bonanno, Supt. of Injection Molding Dept., Composition Novelty Co.; Joseph Pearlman, President, Kwik-Mold Plastic Corp.; Sanford J. Emil, Staff Engineer, Brilhart Plastics Corp.; George M. O'Brien, Asst. Manager of N.Y. Div., U. S. Industrial Chemicals Co., Div. of National Distillers Prod. Corp.; Harold A. Wasserman, Vice-President, Carlyle Industrial Corp.; Leonard Karmel, President, Carlyle Industrial Corp.; Arthur Basescu, President, Bassons Industries Corp.; Charles S. Stryker, Mgr. of Polyester Resin Development, Archer-Daniels-Midland Co.; James C. Bohlen, Director of Industrial Sales, Lunn Laminates, Inc.; George A. Flett, Eastern Area Manager, Rezolin, Inc.

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WHY MONKEYS OBJECT

Three monkeys were dining in a cocoanut tree
And were discussing some things about you and me.
Now what do you think? Now listen you two—
Here, monkeys is something that cannot be true.
That humans descended from our noble race.
Now that is ridiculous; a terrible disgrace.

Who ever heard of a monkey deserting his wife?
Or leaving his babies to starve, or even run "Rife"!
And have you ever heard of a dear mother monk
Leaving her darlings with strangers to "Bunk"?
Human babies are handed from one to another,
Some scarcely know the love of a mother.

And I've never known a monkey so selfish to be
That he would build a fence around a cocoanut tree,
So that other monkeys cannot get even a taste,
But allow the cocoanuts to all go to waste.
Now if I'd put a fence around a cocoanut tree,
I'd naturally expect you to steal them from me.

And there is another thing a monkey won't do.
Get drunk on hard liquor, or get on a "stew"
Carouse, and make whoopy, disgracing his life,
Then reel madly home, and beat up his wife.
They call this all pleasure, and make a big fuss.
They're descended from something, BUT NEVER FROM US!

Bishop A. M. Johnson

P.S.—Any resemblence to persons in the Plastics Industry is an act of God for which Pribble Plastics Products, Inc., assumes no liability.

Wayne I. Pribble

WAYNE I. PRIBBLE

Buffalo

R. W. Miller

The Buffalo Section of the Society of Plastics Engineers met April 15, 1955 at the Park Lane Restaurant, Buffalo, New York. Approximately fifty members and guests gathered to hear Dr. V. T. Stannet, Associate Professor of the College of Forestry, State University of New York, speak on plastic-paper combinations.

Dr. Stannet, a graduate of the University of London, and the Polytechnic Institute of Brooklyn, presented a very interesting talk on the work that has been done at the University. Following are excerpts from Dr. Stannet's talk:

At Syracuse, they are primarily interested in the plastics to paper combination. Some interesting statistics are as follows: 1—20,000,000 lbs. of Urea Formaldehyde were added to paper, in 1954, in percentages of 0.5 -3.0. 2—12% of all polyethylene goes into paper. 3—Polyester resins added to paper give the advantage of continuous run of laminates. Prior to this development, it took high pressures to laminate. Now, with polyester, laminates can be made at fairly low pressure.

Dr. Stannet pointed out that all plastics can be added to paper. It is usually desired to obtain the following properties: 1—Resistance to gas and water vapor. 2—Resistance to water, liquids and grease. 3—Flexibility. 4—Heat and pressure sealability. 5—Chemical resistance. 6—Protection of printing. 7—Scuff resistance. 8 —Electrical insulation. 9—Structural strength. 10—Avoidance of fiber contamination.

It was pointed out that polyethylene has the tendency to absorb flavors and release them later. An alcoholic drink will lose all its flavor when stored in a polyethylene coated container. One mil of polystyrene between two layers of paper results in a product of exceptional dimensional for application in IBM cards. Milk cartons are now being coated with non-toxic resins and will probably replace the wax now being used.

Paper, filled with elastomers such as hycar or neoprene, are being made on paper machines. They have found use for shoe innersoles, and masking tape backings. Elastomer treated papers have the following properties: 1—Wet strength and other wet properties go up. 2—Elongation is better. 3—Better dry strength. 4—More puncture proof. 5—Better chemical resistance. 6—Scuff resistance. 7—Tear and edge tear resistance. 8—Folding endurance. 9—Good sizing properties. 10—Good embossing and molding properties.

Dr. Stannet's talk indicates the field of plastic paper combinations is ever expanding, and will result in better products by the use of plastics.

Eastern New England

A. I. Simon

On May 19, 1955 the Eastern New England Section, SPE, held its monthly meeting at the Quartermaster Corps in Natick, Massachusetts. This was presided over by President Harry Connors of the B. F. Goodrich Company.

There was an excellent turnout with more than 130 present.

Joseph Clancy of Emery Industries, and chairman of the membership committee, introduced a number of new members present. He expressed confidence in having the goal of 200 members attained before the end of the current year.

Ralph L. Mondano of Raytheon Manufacturing Company gave the treasurer's report which included a $200.00 Lowell Technological scholarship obligation. As chairman of the educational and professional activites committee, he expressed the desire to see further funds appropriated for other such worthy educational projects.

SPE national director and SPE local president Richard Beckwith of Dow Chemical commented on his trip to Houston and other national SPE activities. He mentioned that publications of preprints of the SPE Atlantic City Conference have been donated to libraries of several universities.

After the usual drawings of the door prizes which were donated by Kopper Company and M. W. Kellogg Company, David Towler of the Kellogg Company and chairman of the program committee introduced Dr. Oesterling of the Natick Quartermaster Corps Research Labs. Dr. Oesterling gave an outline of the various groups at and the types of projects being uadertaken by the Natick Quartermaster Corps Research Labs. He offered the facilities of the Laboratory's dining hall and auditorium for any future SPE meetings.

Dr. Murray, in charge of the Plastics Group at the Quartermaster Corps, presented some of the problems with which his group is working. Some of these included arctic plastic skis and snowshoes, and abrasion-resistant plastic dishware.

After the meeting, a very interesting guided tour was made of the various laboratories and facilities of the Quartermaster Corps in Natick.

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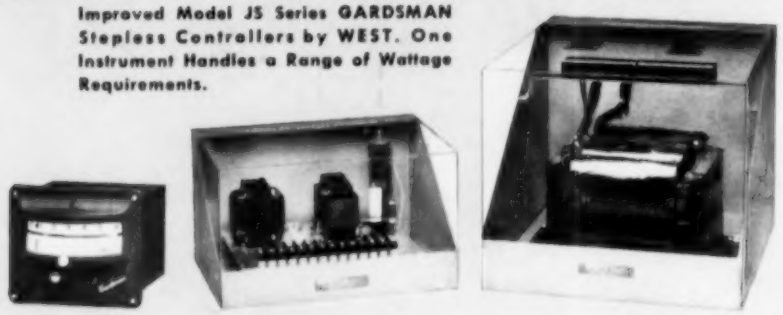

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Miami Valley

M. V. Roode

The May meeting of the Miami Valley Section Society of Plastics Engineers was marked by a very informative, slide illustrated talk on "Coatings on Plastics" by guest speaker, Dr. Lloyd E. Parks, Vice President and General Manager of Logo Inc. of Chicago.

After a tasty swiss steak dinner at Schulyers Restaurant in Cincinnati, the 28 members and guests present heard Dr. Parks, introduced by our President, Joe Overwein, outline the problems involved in the formulation of pigmented coatings and in their application to various types of plastic materials.

Any pigmented coating for plastic parts consists essentially of two systems, resin and solvent, each of which must be chosen with great care and then tailored to the specific application, said Dr. Parks.

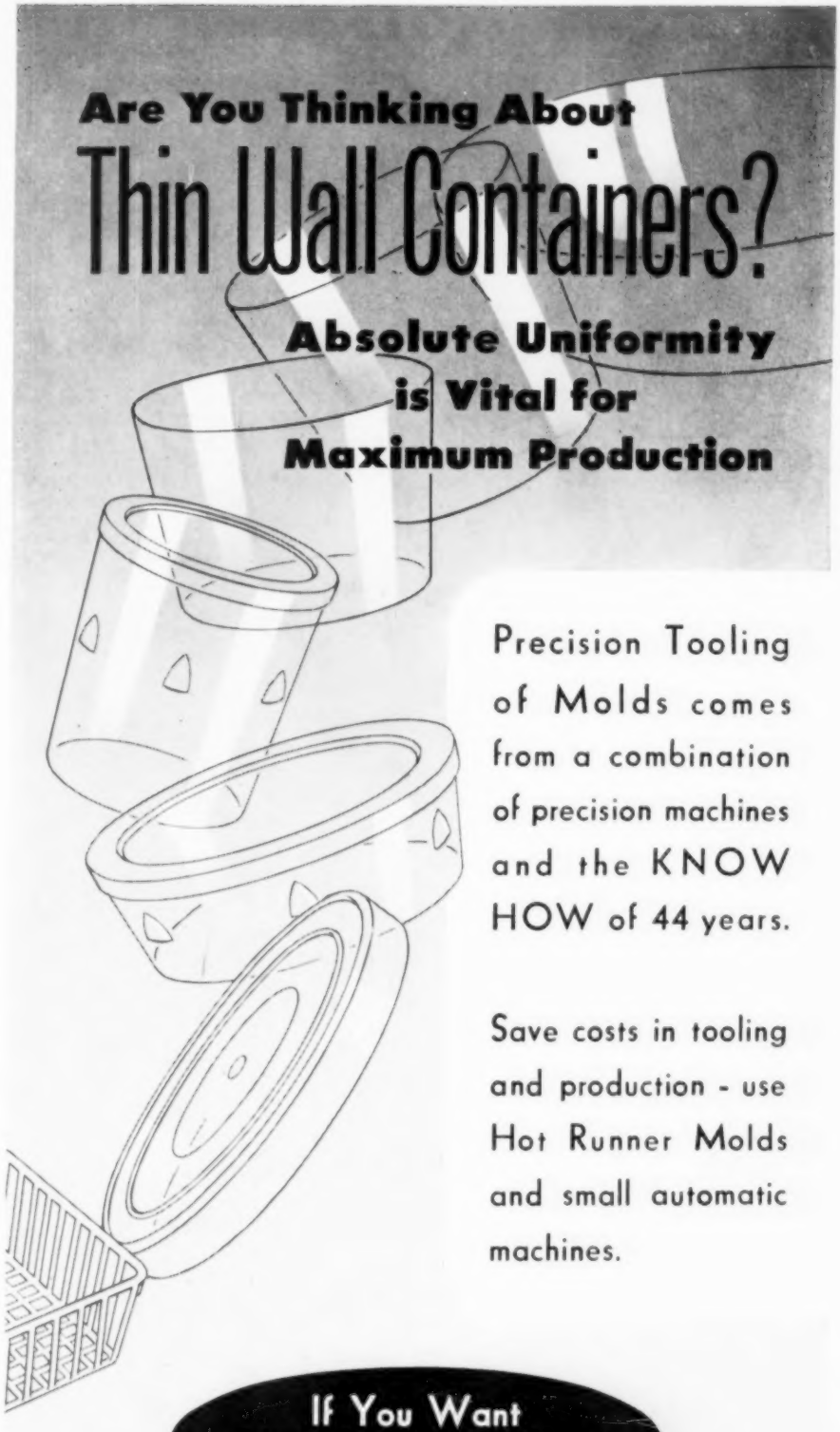
The requisite of a good resin system is its film forming characteristics, its pigment carrying ability, its flow, and its adhesion to the type plastic being coated.

Adhesion is probably the most fundamental property to be sought in a resin system. After good adhesion is obtained, the system may be modified with other resins to obtain other specific properties, although care must be exercised to retain the same quality of adhesion.

Equally important is the solvent system, necessary for the complete solvation of the resin system, yet which will not attack the plastic part to give crazing or etching. Other effects of improper balance of the solvents in the system may appear as orange peel, cratering or humidity blush in the applied coating.

Humidity blush may very easily occur on a wet day because the extra moisture in the atmosphere may affect the solvent balance to the extent that it will no longer hold the resin system in complete solvation. It is also possible, however, that in some instances humidity can be somewhat of an asset. High humidity may reduce the strength of a solvent in its attack on the plastic, thereby reducing crazing.

The method of application of the coating must also be considered in the formulation of the solvent system. A blend of high and low boiling solvents is used for spray coatings where a good portion of the low boiling solvent may be blown away in the spraying, thus increasing the solid contents and the viscosity of the wet paint film laid down. Yet, it is the high boiler that controls this viscosity of the wet film and must hold the resin in solution until the film is completely dry regardless of the method of application.



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Toledo

D. E. Cordier

The May meeting of the Toledo Section was held at the new Toledo Express Airport on May 25th, with approximately 35 members and guests being present for dinner in the Airport dining room.

Following the dinner, the Section was privileged to enjoy a tour of the Airport control tower and an explanation of the various navigational devices and facilities in operation there.

New Section members were announced as follows: Mr. Don Basilius (Basilius Grinding and Tool), Mr. Charles Bellg (Plastics Tooling), Mr. John Larimore (Barrett Division), Mr. Carlton Richie (Owens-Illinois), Dr. Bobb Santelli (Owens-Illinois), Mr. Pete Swartzbaugh (Peerless Molded), and Mr. Al Tomaszewski (Peerless Molded). These accessions represent a definite step towards President Bowlin's stated goal of 80 members for the Toledo Section.

Mr. Jean F. Malone of B. F. Goodrich Chemical Company, was then introduced by Program Chairman Frank Rosenberger. Mr. Malone's subject was "Rigid Polyvinyl Chloride and Its Applications." Mr. Malone traced the history of rigid polyvinyl chloride from its commercial introduction in this country about 1950, through a usage of about 250,000 lbs. in 1952 and 2,500,000 lbs. in 1954, to an estimated 25,000,000 lbs. in 1955. This impressive performance has derived from a combination of outstanding chemical resistance, good physical and thermal properties, and satisfactory processing characteristics which have opened up opportunities as a structural material in the piping, building material and lighting fixture industries.

Engineering performance data relating to long time safe working stresses at 80°F. and 100°F. were presented to assist the designer in utilizing both Type I and Type II rigid PVC for structural uses.

In the building industry the use of rigid polyvinyl chloride for corrugated sheeting for internal partitions, opaque wall panelling and lighting fixtures was described. Rigid PVC corrugated sheeting is not recommended for external use because of the color degradation brought about by the ultra-violet component of ordinary sunlight. It should be noted, however, that one year Florida weathering tests on pipe failed to bring about any significant decrease in strength.

Mr. Malone presented the limitations of rigid PVC related to temperature range, service conditions and the specific materials to be handled, and also discussed the processing, fabrication and joining of the material.

A lively question-and-answer period concluded Mr. Malone's presentation of an interesting and significant new plastic material.

The summer golf meeting of SPE was held on June 29 at Chippewa Country Club at which time several members participated in golf and a putting contest.

A very short business meeting was conducted and the meeting was then turned over to Jim Hyland who was chairman of this particular outing.

Prizes were given for the various golf scores as well as attendance prizes at which time the meeting adjourned.

Milwaukee

Marvin C. Stowe

The Milwaukee Section had on June 11 what will be known from here on in as its Annual Summer Party. It was an all out affair with cocktails, dinner, dancing. Couples were encouraged which gave the party a very sociable atmosphere. Sixty members, guests, and wives attended.

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DETROIT

Mac Nichols

The 1955 "Spring Golf Gambol"—and you can spell it anyway you wish—was held on May 24th at the Plum Hollow Golf Club, and the Divot Diggers of the Detroit Section were out en masse.

The event, directed by Jay Kuhn the Program Chairman of the Chapter, was attended by 91 SPE members who brought with them 38 guests.

Seventy-three aspiring Sam Sneads started teeing off in the early afternoon and by dusk, the course looked like a plowed Iowa cornfield just prior to planting.

In the evening after the banquet, Pat Luckino, Chairman for the Day, presented the golf and door prizes. The Golf Kicker's Handicap was won by Russ Stanhope of Micromatic Hone and Mrs. Stanhope can go back and tell her nosy neighbors that it was not the Paddy Wagon that brought Hubby home—but a Brink's Armored car.

The MacDonald Manufacturing Company of New Baltimore donated a handsomely engraved Silver Bowl to be known as "The Stuart MacDonald Golf Trophy" which is to be given as a perpetual award to the Detroit Section golfing enthusiasts. Each year the low gross scorer in the Spring and Fall Tournaments will retain the trophy. Competition for "The Bowl" will be limited to SPE members of the Detroit Section.

Iver Freeman, the President of the Detroit Chapter, presented a plaid Nylon golf bag that was equipped with everything but oar locks, to Past-President Carl Whitlock as a token of the esteem and regard held for him by the Detroit Section during his term in office.

Effect of Fillers . . .

(Continued from page 46)

fillers such as powdered calcium carbonate, mica, asbestos, and wood flour. In general, fillers increase the shear modulus and decrease the damping over that of pure polystyrene. The heat generated by a unit deformation of the material, however, usually is increased by a filler. The results can be partially explained in terms of the individual mechanical properties of the polystyrene and filler and by an "adhesion factor."

REFERENCES

1. L. E. **Nielsen,** Rev. Sci. Instruments, **22,** 690 (1951)
2. C. Zenzer, "Elasticity and Anealasticity of Metals," University of Chicago Press, 1948.

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Man to manage medium size plastic molding dept. Must have supervisory experience in both compression and injection molding. Must be thoroughly familiar with and capable of supervising and advising on all problems relating to plastic molding dies and equipment. This is an invitation for an experienced, high caliber man to join a sound organization with a good background and an excellent future. Contact Mr. Evans or Mr. Thesis at

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Man between 30 and 45 years old to supervise engineering operations in a modern four machine injection molding plant with an established enviable record.

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Devoe and Raynolds Company, Inc., THE PIONEERS in the Epoxy Resin field is diversifying by expanding its activities to include the field of laminating, potting, tooling, adhesives, etc. A new well equipped laboratory unit is being established to take advantage of our broad and basic experience in Epoxy Resins. This new laboratory requires key personnel to form the nucleus of this group and grow with us in this expansion program.

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PATENT FOR SALE

U. S. patented July 1955, Canada Patent pending. 3 parts, entirely made of plastic. Modern style with granted claims is completely worked out. Write to G. von Elm, 382 Lipton Ave., Winnipeg, Manitoba, CANADA.

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One used 32-ounce Lester Injection Molding Machine in good operating condition. New in 1950. STANLEY BERG & CO., Frick Building, Pittsburgh, Pa. Phone —EXpress 1-3535.

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Manufacturers' representative, well financed, wants industrial plastics item line with good profit potential. Box 1155, 34 East Putnam Ave., Greenwich, Conn.

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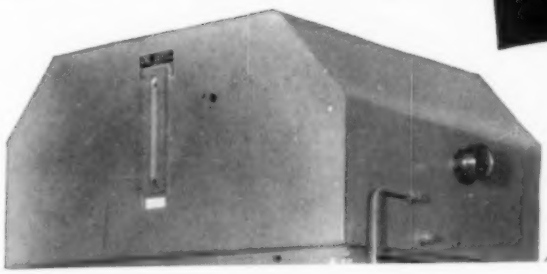

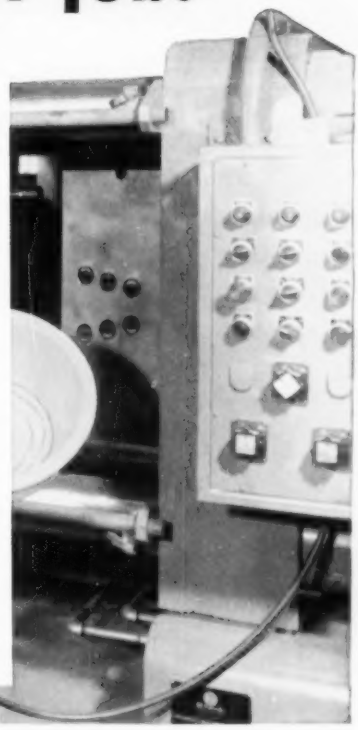

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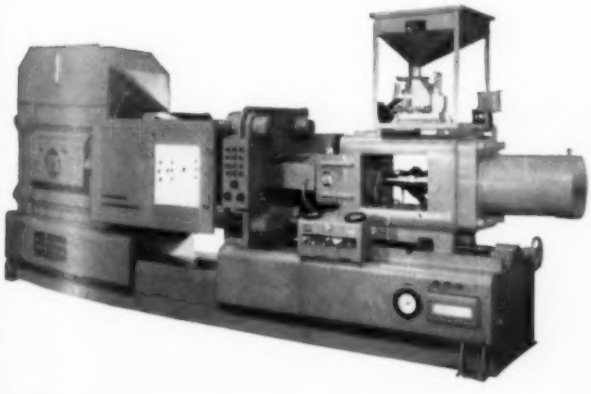

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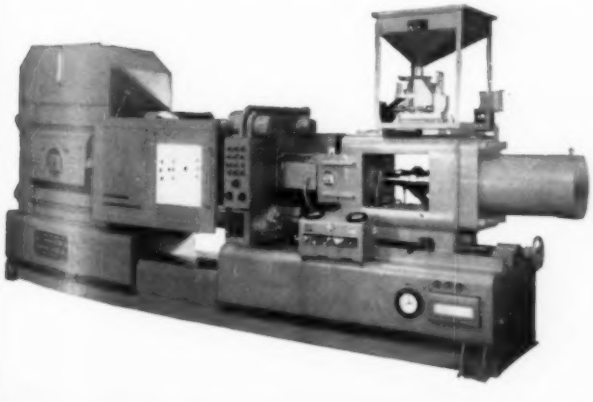

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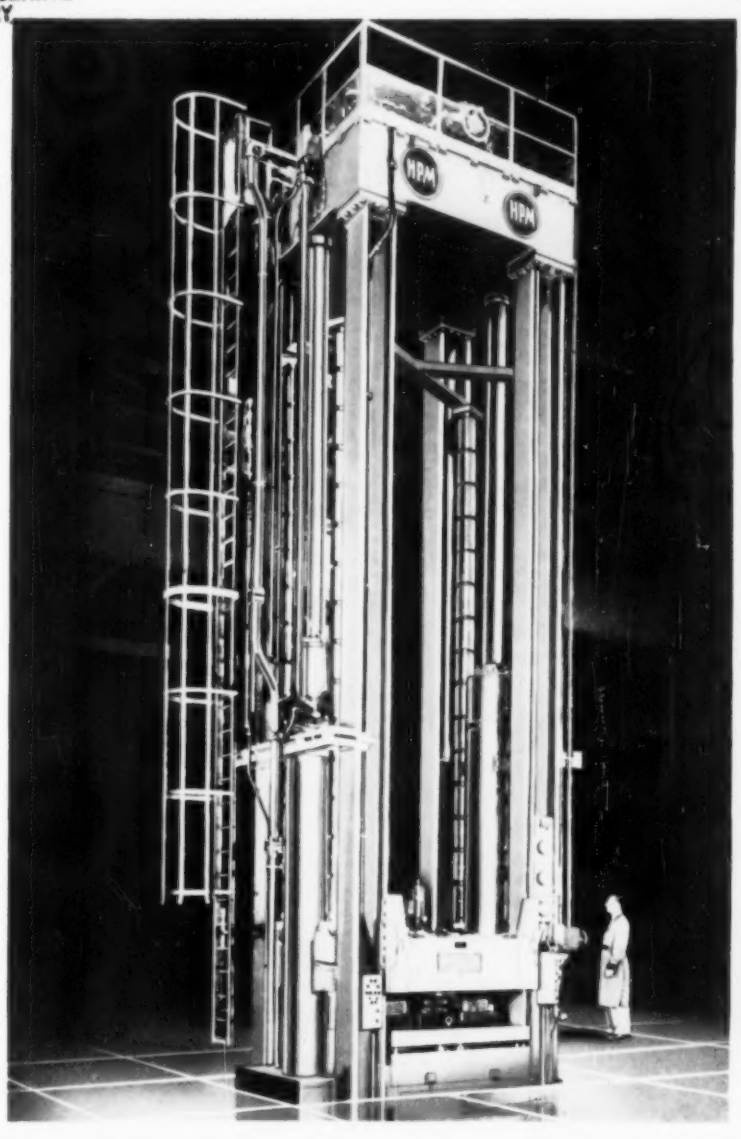

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Vol. 11 September, 1955 No. 7

Published monthly except July and August by

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30 cents a copy $3.00 per year

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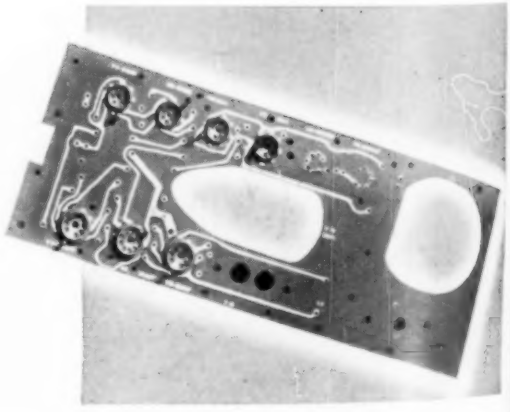

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Published monthly except July and August by

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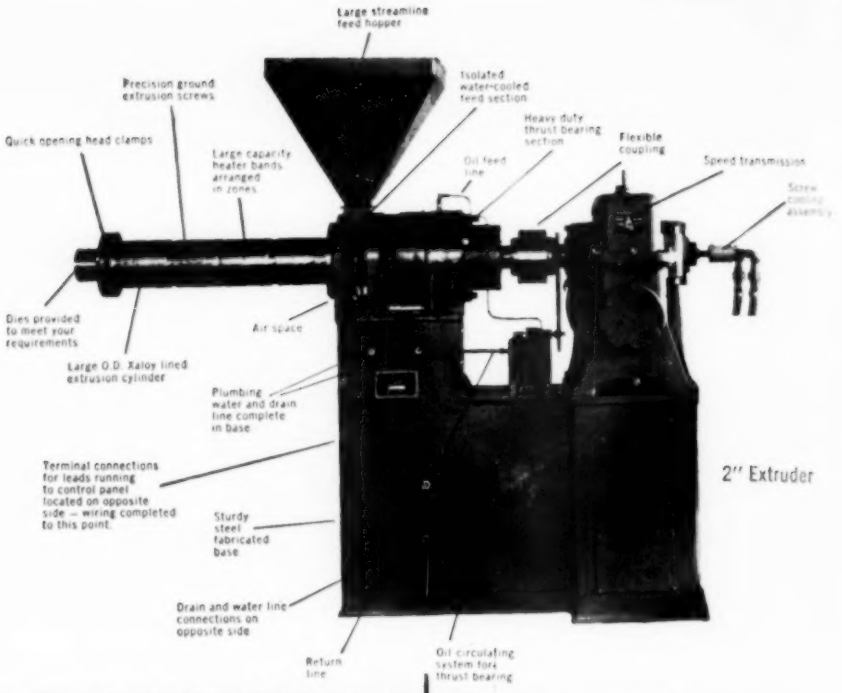
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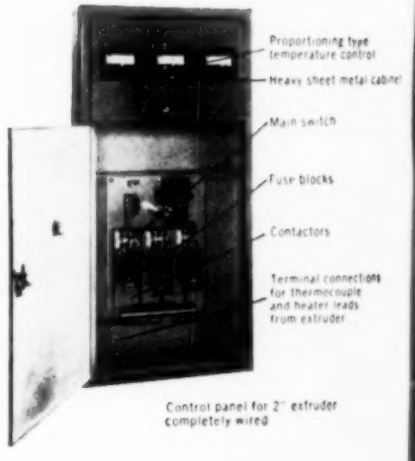

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Zones of Heating on Cylinder	2	2	3	4	4	4
Maximum Heating Load, KW	12	18	30	48	96	160
Thrust Bearing Capacity, 1000 lbs. @ 50 rpm.	60.5	60.5	119.5	208	288	420
Type Speed Transmission	Worm gear.	Worm gear.	Herr' bone	Herr' bone	Herr' bone	Herr' bone
Usual Motor Size, H. P.	7½-10	10-15	20-30	40-50	50-75	75-150
Approx. Extrusion Capacity lb./hr.	20-50	40-90	75-200	150-450	250-650	450-1200
Approx. Floor Dimensions—inches	40x98	48x112	60x120	84x132	96x156	104x192
Approx. Weight—lbs. (less Drive)	3200	3600	4800	6600	9600	12500

**Extruders are modified to meet requirements for a particular process*

Twelfth SPE Conference To Be Held in Cleveland

The 12th annual Conference of the Society of Plastics Engineers will be held in Cleveland, Ohio at Hotel Statler in 1956, according to a statement made by Frank A. Martin, general chairman of the Executive Committee. The conference extends from Tuesday, January 18 through the 20th.

Approximately 2,000 members of SPE are expected to attend the technical conference. Registration will begin Tuesday afternoon, January 17, with talks starting Wednesday morning and extending through Friday. The annual business meeting and luncheon will be held Wednesday, and a banquet is scheduled for Thursday evening.

Subjects to be covered during the meeting are these phases of plastics engineering:

Injection Molding, Technique and Machinery; Compression and Transfer, Technique and Machinery; Extrusion, Technique and Machinery; Calendering and Casting, Technique and Machinery; Reinforcement (by fibers, cloth—by elastomers); Foams; Professional Activities and Education; Physical Properties (Methods, Comparative, Correlations, Creep); Research; Defense Applications of Plastics; Automotive and Aircraft Applications; Rigid Plastics; Application and Processing; General or Unassigned.

Papers on the above subjects have been approved and preprints on these discussions will be given to each registrant.

Details of the meeting are being arranged by the Executive Committee and special committee chairmen. The following men compose these committees:

General Chairman, Frank A. Martin, The Hoover Company; *Co-Chairman*, Sherman W. Crawford, Detroit Mold Engineering; *Treasurer*, Ernest P. Moslo, Moslo Machinery; *Secretary*, Jack Pectal, Eastman Chemical Sales.

Committee Chairmen: *Speakers*, William E. Manring, B. F. Goodrich Chemical Company; *Program*: Hugh Winn, Firestone Tire and Rubber Company; *Registration*, Harry Zimmerman, B. F. Goodrich Chemical; *Publicity*, Samuel A. McElroy, Hercules Powder; *Sergeant at Arms*, R. B. Treer, Warner and Swasey; *House*, Harold Olson, Harshaw Chemical; *Printing*, A. R. Morse, Injection Molders Supply; *Advertising*, Barney Wolfe, Injection Molders Supply; *Banquet*, Kenneth M. Keller, Keller Products; *Company*, R. C. Lopez, H. Muelstein; *Advance*, H. D. Oliver, Jr., Eastman Chemical Sales; *Ladies*, Miss Elizabeth Goodine, Geauga Industries; *Prizes*, A. Edward Campi, International Molded Plastics; *Finance*, Ernest P. Moslo, Moslo Machinery.

Rooms and suites are available at the Hotel Statler. During the meeting, tours and fashion shows will be planned for the wives of SPE members.

For further information please contact S. A. McElroy, Hercules Powder Company, 865 Union Commerce Bldg., Cleveland, Ohio, SUperior 1-4335.

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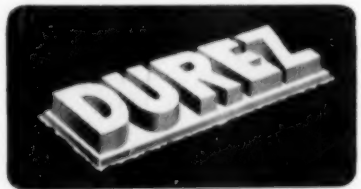

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- INDUSTRIAL RESINS
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PHENOLIC PLASTICS FOR THE NEW COMPETITIVE ERA

Book Reviews . . . .

Chemical Business Handbook, prepared by a staff of specialists, John H. Perry, Editor in Chief. Over 1,300 pp. $17.00 McGraw Hill Company, Inc., New York, First Edition, 1954.

124 authors contributed to this book under the editorship of John H. Perry, editor of *Chemical Engineers Handbook*, who unfortunately died while this book was on the press.

It is divided in 20 sections which have each their own pagination. The first three sections deal with business, finance, management and control and commercial chemical development. They contain selected financial and business data, describe accounting methods and marketing and development procedures. (Many readers will object to Carl Pacifico and Thomas H. Vaughn that they do not make any difference between commercial laboratories and professional consultants.) The following three sections are devoted to research. Section 6 contains 282 pages of valuable data and sources of information on market research such as annual production of a great many chemicals, use of these chemicals, consumption data and market research.

The commercial aspects of chemical manufacturing are treated in sections 7 to 15: purchasing, production, transportation, sales, advertising, collections, personnel management, public relations and business law. These chapters initiate the technical man and the executive to management in the office, the plant, the laboratory and in public relations. The following sections are highly

(Please turn to page 8)

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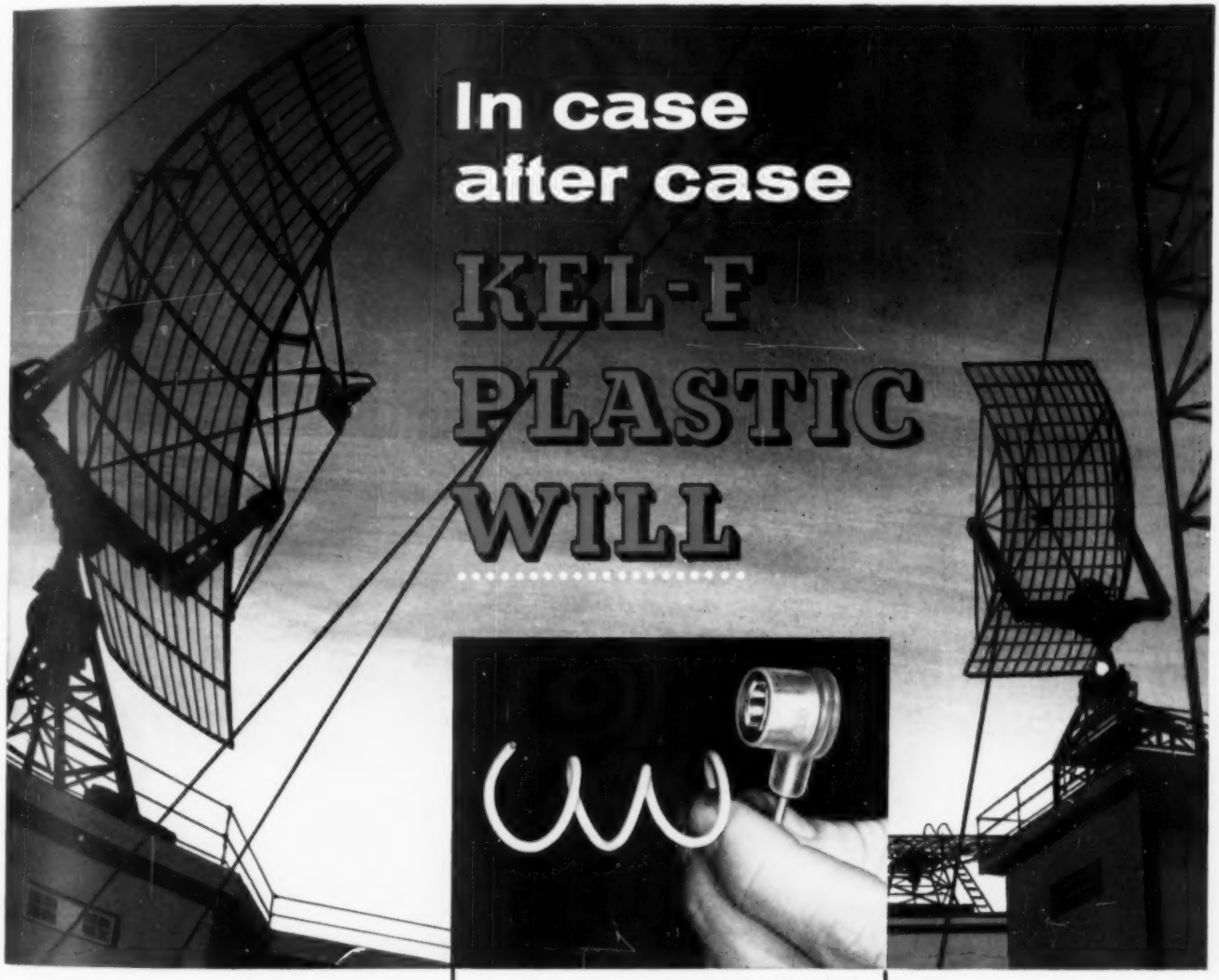

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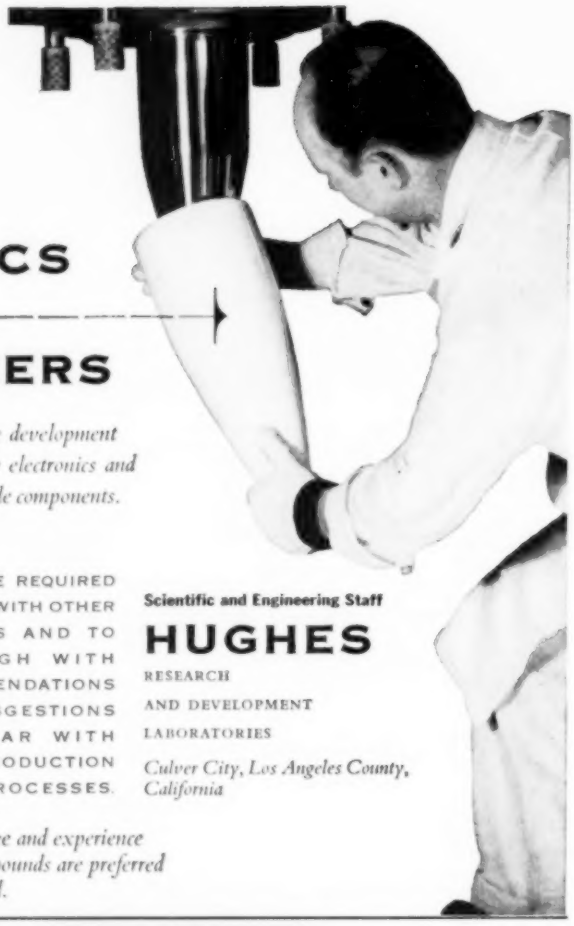

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Book Reviews . . .

(Continued from page 6)

specialized. Section #16 deals with patents and patent laws and certainly belongs in a book of that kind. Section #17 is devoted to industrial toxicology and gives in a few pages the elements of internal and external toxicity, toxicological investigations and tests which all chemists should know. Section #18 concerns insurance and loss prevention and will be of interest particularly to plant managers and heads of personnel departments. The last two sections contain reports, report writing and business mathematics. They give data which will greatly help executives dealing with reporting, operations research and statistics.

It is obvious that this handbook will be indispensable to beginners in the chemical business. Furthermore, we believe it will be of great value also to technical men and executives in the many problems which confront them in their diversified duties. This encyclopedia of outstanding data, some of them not readily available elsewhere, should be on every chemist's desk.

Dr. Louis C. Barail

PLASTIC TOOLING, Malcolm W. Riley; Reinhold Publishing Corporation, New York, 123 pages.

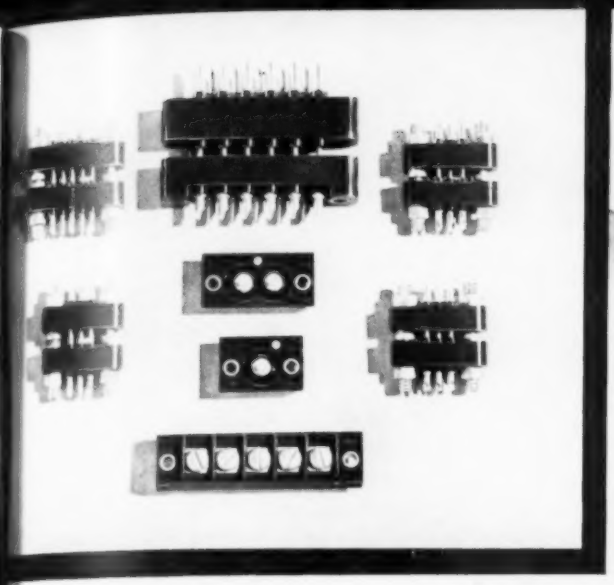
This is a Reinhold Pilot Book, written to give a concise and timely presentation of the growing plastic tooling field.

The book deals primarily with methods, materials, and applications of plastic tooling in industry. Fundamentals, economic aspects, and properties are briefly discussed.

This book can properly be termed a handy reference book for engineers, shop personnel, and those contemplating entering this field or using plastic tools. The author takes the stand that plastic tooling is not a substitute for metal tooling but proven alternate materials, useful in their own right, with definite possibilities, limitations, advantages, and disadvantages. This thinking is reflected throughout the text which covers such subjects as tooling resins, fabricating, checking fixtures, assembly fixtures, metal forming tools, plastic forming tools, die models, and prototypes. A chapter is also devoted to the future of experimental tools. The book has 10 chapters in all, together with a number of pictures and illustrations.

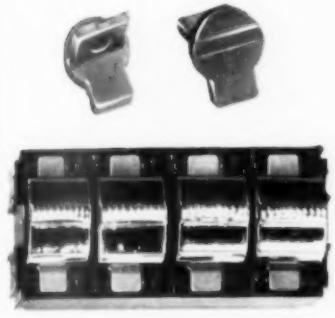
Ronald D. Beck

MOLD AND DIE MAKING FACILITIES: Bulletin describes the more than 150 pieces of capital equipment including grinders, milling machines, duplicators, lathes, presses, etc., this firm used in producing and testing custom molds, dies and hobs for plastics processors. Newark Die Co., 20 Scott St., Newark, N. J.



Multi-contact connectors molded of Cyanamid's Melamine No. 592 by Armel Electronics, Inc., Brooklyn, N. Y.

Standoff terminals molded of mineral-filled Melamine No. 592 by Whitso, Inc., Schiller Park, Ill.



Automobile window-lift plungers molded by Whitso of cellulose-filled Melamine No. 1500.

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Though names may be forgotten, the world still remembers and admires the craftsmanship of the artisans of other ages.
Today, the selfsame skills that created beautiful and useful objects of wood, silver and steel are working with a new kind of material—an exciting family of plastics.
Pictured here are two of the specialists who are creating a great variety of products of plastics to serve every industry, every home.
Monsanto, a major producer of high-quality plastic materials, salutes these modern craftsmen who are helping to mold America's tomorrow.

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George J. Geretschlaeger, Minnesota Plastics Corporation, St. Paul, Minn. For 15 years Mr. Geretschlaeger has been learning his craft at every operational level. Today, as Molding Superintendent, he applies his first-hand knowledge of molds and materials to every job. Recently, by designing a trimming fixture that did away with hand de-gating, he stepped up production on a washer for casement windows from 2,500 to 4,500 pieces an hour. Says Mr. Geretschlaeger: "It takes the combined talents of many people with long experience in the various phases of injection molding to produce quality plastic parts at minimum cost."

Fred J. Kirk, Kirk Molding Co., Clinton, Mass. A background of automotive and aircraft skills has shaped Mr. Kirk's philosophy that "producing the best products at lowest cost is a matter of exacting the best performance from machines and materials." As his company's owner, he has won customer confidence by equipping his plant with some of the finest automatic equipment available in the industry. Specializing in custom molded packages for products such as silverware and precision power tool components, Mr. Kirk contributes his personal attention and 14 years' experience in injection molding engineering to every job.

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PAC STEPS FORWARD

by Don F. Hoffman, PAC Administrator

After fourteen years of struggle for the organization of the membership into groups of specific branches of plastics engineering* the Society is now reaching toward success through the work of PAC. Progress has already been made, but contributions to the work of PAC by every SPE member are necessary to obtain the objective.

The overall organization of PAC was presented to you in chart form in the May '55 SPE JOURNAL. At the June PAC Meeting in Minneapolis additions to the organizational chart and some revisions in terminology were made.

The full name of PAC is now the Professional Activity Committees.

Miscellaneous projects under Mr. Jerome Formo have been reclassified as Special Technical Projects.**

Geographical names have replaced the numerical system of the PAC Engineering Volumes Steering Committee as follows:

Region I	—New England	Region V	—Midwest
Region II	—St. Lawrence	Region VI	—Gulf
Region III	—Atlantic	Region VII	—Pacific
Region IV	—Ohio-Pittsburgh	Region VIII	—Central

Mr. John Delmonte has been named Chairman of the Pacific Region Engineering Volumes Steering Committee.

It is expected that membership groups of specific branches of plastics engineering will be developed as a concurrent result of the work now started by the PAC Technical Volumes Steering Committee.

At the June meeting of this committee work was assigned for re-editing the book, "Injection Molding." Chapter assignments have been made to the eight PAC regions as listed. Any qualified member desiring to help re-edit this book is requested to contact his Section's National Director.

Chapter 1 — History, New England. Chapter 2 — Development of Injection Molding Techniques, New England. Chapter 3 Materials for Injection Molding, Atlantic, Ohio, Pittsburgh. Chapter 4 — Molding Practices, Atlantic and Midwest. Chapter 5 — Product Design, Pacific. Chapter 6 — Mold Design and Construction, Central. Chapter 7 — Finishing and Decorating, Midwest. Chapter 8 — Estimating and Costs, Gulf. Appendix I and II Physical properties and general data, St. Lawrence.

"Injection Molding" is scheduled for republishing June, 1956.

It has been decided to point this and all PAC technical publications to the professional or post graduate level so as to make these works of interest particularly to engineers and other technical personnel.

Regional editors for this publication are being named and a full report is expected at the September 12 meeting of the Steering Committee in Cleveland.

The Technical Project Committee on Quality Control, Dr. L. Debing, Chairman, has completed its assigment of preparing and publishing a complete series of papers on the use of quality control in plastics. The next step in this committee's work will be to publish the papers in a collected volume with whatever minor re-editing may be required for this purpose. It is the committee's hope to have this published late this year.

The Cooperative Technical Programs Committee under J. H. DuBois has made assignments to members of SPE who will act as our official representative in work done by other technical societies. It is expected that the complete list of liaison men will be published in the very near future.

Recent acceptances by ASTM Committee D-20 on Plastics of SPE Representatives are:

Name	SPE Section	ASTM Subcommittees
N. L. Perry	New York	V, XV, XVIII, XIX
H. H. Dash	New York	VIII, X, XVIII
E. N. Dorman	New York	Not selected as yet
F. J. Donohue	Western New England	VII, IX, XVI
C. F. Massopust	Chicago	Not selected as yet

SPE's liaison member with the International Standards Organization, Dr. Norman Skow of the Philadelphia Section, attended the ISO/TC61 Plastics Committee meeting in Paris July 8 to 13.

Dr. Skow reports that sixty-eight delegates were in attendance representing the following countries: United States, United Kingdom, France, Germany, Switzerland, India, Sweden, Belgium, Italy, Netherlands, and Czechoslovakia.

The U. S. was represented by Dr. G. M. Kline of the National Bureau of Standards, Mr. Robert Burns of the National Research Council, Dr. Norman A. Skow of the Synthane Corporation, Mr. W. A. Frantz of E. I. duPont, Mr. R. R. Winaus of the New York Naval Shipyard, Mr. E. Y. Wolford of the Koppers Company, Mr. C. H. Adams of Monsanto Chemical Company, Mr. J. W. McNair of the American Standards Association, Dr. R. K. Witt of Johns Hopkins University and Mr. E. E. Ziegler of Dow Chemical Company. Dr. Kline served as President of the meeting and Dr. Skow as Secretary. Mr. Burns was the Leader of the U. S. delegation.

Plenary meetings were held at the beginning and end of the five day session but most of the time was devoted to smaller meetings of the eight working groups. The working groups covered the following problems:

Working Group No. 1 — Nomenclature & Definitions

Working Group No. 2 — Mechanical Strength Properties.

Working Group No. 3 — Standard Laboratory Atmospheres and Conditioning Procedures

Working Group No. 4 — Thermal Properties

Working Group No. 5 — Physical Chemical Properties

(Please turn to page 12)

*SPE Constitution Article XII — Professional Activity Groups

**SPE Constitution Article XIII — Technical Committees

National Organization . . .

(Continued from page 11)

Working Group No. 6—Ageing, Chemical & Environmental Resistance

Working Group No. 7 — Preparation of Test Specimens

Working Group No. 8 — Review of Methods for Electrical Properties.

A more complete report on the actual accomplishments of these working groups will be made in the future. Dr. Skow reports that it was gratifying to see how diligently the members of the working groups tackled their problems and to observe the spirit of genuine cooperation. When their work is finally complete it will lead to more friendly relationships between the various countries as well as to a better understanding of the products sold in inter-country trade.

The meeting ended in a mutual feeling that a difficult problem is well on its way to solution.

Liaison work is anticipated with other societies such as American Society of Safety Engineers, National Society of Corrosion Engineers, American Association of Mechanical Engineers, Manufacturing Chemists Association, SPI, Building Research Institute, American Standards Association, and many others.

Regional Technical Conferences — This also is a new committee. Established at PAC's June meeting, it shows promise of fast progress.

(This report will be continued in the October issue.)

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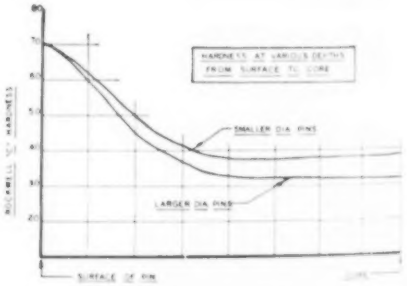

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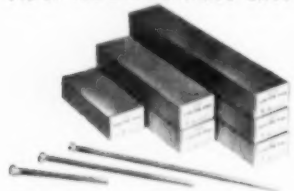

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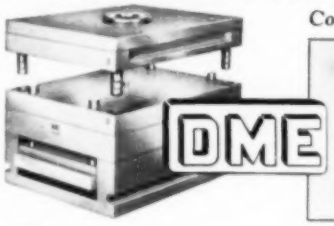

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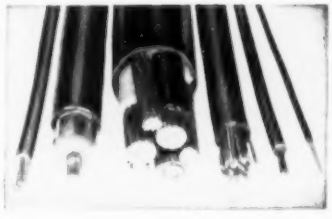

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	73 °F.	lb./sq.in.	D412-51T	1,750
	158 °F.	lb./sq.in.	D412-51T	850
	−70 °F.	lb./sq.in.	D638-52T	4,750
	73 °F.	lb./sq.in.	D628-52T	1,900
	158 °F.	lb./sq.in.	D638-52T	950
Yield point	−67 °F.	lb./sq.in.	D412-51T	4,800
	73 °F.	lb./sq.in.	D412-51T	1,700
	158 °F.	lb./sq.in.	D412-51T	850
Elongation	−67 °F.	%	D412-51T	60
	73 °F.	%	D412-51T	600
	158 °F.	%	D412-51T	600
Modulus of elasticity	73 °F.	lb./sq.in.	D638-52T	35,000
Stiffness	−67 °F.	lb./sq.in.	D747-50	230,000
	73 °F.	lb./sq.in.	D747-50	27,000
	158 °F.	lb./sq.in.	D747-50	8,000
Shear strength	73 °F.	lb./sq.in.	D732-46	2,750
Impact strength, Izod	−40 °F.	ft.lb./in.	D256-47T	0.8
	73 °F.	ft.lb./in.	D256-47T	did not break
Hardness, Shore			D676-49T	C81,D48

THERMAL PROPERTIES			
Thermal conductivity	B.T.U./hr./sq.ft./°F./in.	Cenco-Fitch	1.75
Heat-distortion temperature, 66 lb./sq.in.	°F.	D648-45T	121
Softening point, Vicat	°F.		201
Brittleness temperature	°F.	D746-51T	<−105

ELECTRICAL PROPERTIES	Units	Test method	
Dielectric constant 60 cycles to 100 megacycles,		D150-47T	2.3
Power factor, 60 cycles to 100 megacycles		D150-47T	0.0005

MISCELLANEOUS PROPERTIES			
Flammability	in./min.	D635-44	0.94
Specific gravity		D792-50	0.92
Water absorption	%	D570-42	0.014
Melt index		D1238-52T	2.1
Index of refraction	n_D	D542-50	1.51
Mold shrinkage	in./in.		0.02-0.05
Compression ratio		D392-38	2.1–3.6

NOTE: Data shown are average values of molded "Alathon" 10. These values should not be used for specifications.

ZYTEL® nylon resin · **ALATHON® polyethylene resin** · **TEFLON® tetrafluoroethylene resin** · **LUCITE® acrylic resin**

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TEMPERATURE CONTROL UNITS

N. Y. Reinforced Meeting

Joel Frados

The Reinforced Plastics Group of SPE's New York Section held its regular dinner meeting on May 25 at Tufaro's Restaurant, Corona, with 57 members and guests attending. Robert J. Brinkema, Brinkema Plastics, served as moderator of a five-man panel on "Tool Design in Molding Reinforced Plastics."

John C. Moricoli, C. L. Gougler Machine Co., opened the panel talks with a discussion of "Machining of Matched Metal Molds for Molding of Reinforced Polyester Resins." He outlined six basic steps imperative for high quality mold making: pre-design planning, mold design (layout), approval of mold design by the molder, purchasing of mold steels, detailing of mold design, and pre-machining conferences between engineering and production. Mr. Moricoli also made recommendations concerning steels, hardening of molds, chrome plating, finishes, tolerances, shear edges, and standardization of inquiries.

"Cast Aluminum Tooling" was the subject of a talk by John Avignone, M. A. Cuming & Co. Differential expansion in steel and aluminum limits molds designed from the latter material to the resin seal type, straight compression, or a landed mold. Heating and cooling advantages of cast-in tubing can be utilized in aluminum castings due to the metal's high conductivity. Molds should have uniform wall thickness with additional strength possible through rib constructions and annealing. Ease of machining and lightness of weight are reported economy features.

The Kellering process was outlined by Milton Hoffman, Victor Kellering, Inc. Victor Kellering machines are used as automatic tracers for controlled molding and work on an electrical principle. Close tolerances (five mils) on material thickness are possible; however, speed is difficult to obtain. The process is used essentially to obtain accurate matched metal molds, and is not intended to compete with the two aforementioned methods.

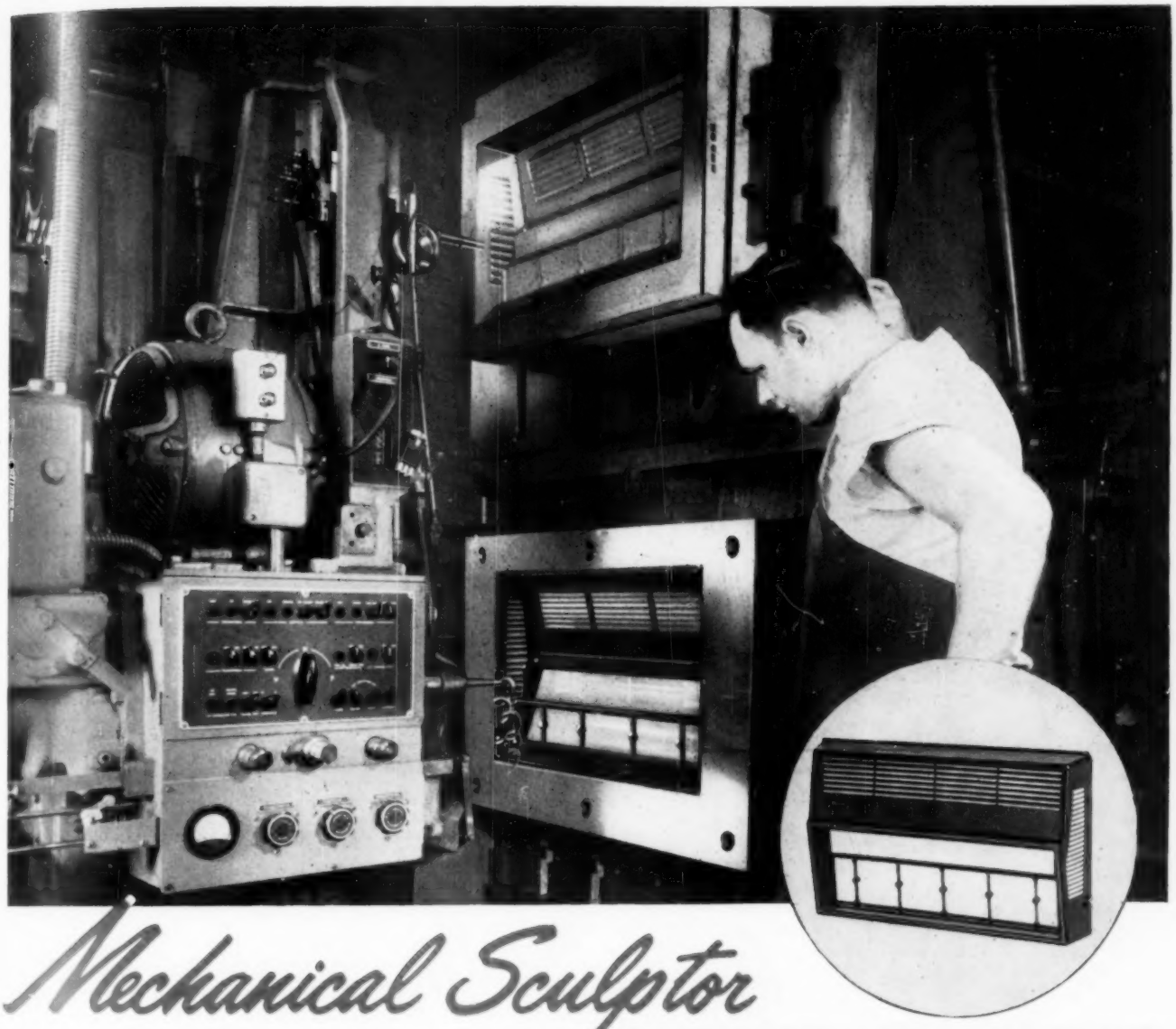
"Design of Matched Metal Molds from the Molder's Viewpoint" was presented by Sidney D. Yarm, Bassons Industries Corp. He listed the following considerations as essential to mold design: quantity of parts required, part size, complexity of part, surface finish desired, whether trimming and blanking are to be performed in the molding operation, likelihood of design changes, and the type of delivery schedule involved. Advantages of aluminum, cast iron, Meehanite, and steel molds were considered. A major difficulty encountered by the molder in working the molds is the securing of proper draft; one degree minimum being required.

Frank Nussbaum, Riverside Plastics Corp., described bronze molds as releasing well without inhibiting the cure. Expensive, they best serve in molding situations requiring intricate shapes and indefinite wear.

Mold release problems with steel, bronze, and aluminum were considered by the panel, and the following conclusions reached: after polishing, chrome plating, and buffing serve well for parts requiring painting; porosity is limited by impregnating the mold with a parting agent having a non-aqueous base; and molds are best cleaned with paper which is immediately thrown away.

Deflashing Bulletin Available

Deflashing of thermosetting molded plastics at high speeds by means of automatic methods is discussed in a new bulletin just published by American Wheelabrator & Equipment Corp., 1258 S. Byrkit St., Mishawaka, Indiana.



Mechanical Sculptor CARVES BIG MOLDS

With feather-light touch, the tracer of this huge die sinking machine passes over the surface of a master pattern. The slightest change in contour is transmitted by a varying electric current to the cutter, which faithfully reproduces every intricate detail of the pattern in the mold cavity.

From the mold shown, Bridgeport Moulded Products Co., Inc. produced the handsome grille of the Philco air conditioner pictured in the inset — one of the hundreds of fine products for which molds have been created by the skilled hands and modern facilities at Newark Die Company.

Big molds have been a big factor in the growth of the plastics industry. For more than 30 years Newark Die has led in the design and construction of all types of molds.

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Testing section of Dow Plastics Coatings Laboratory.

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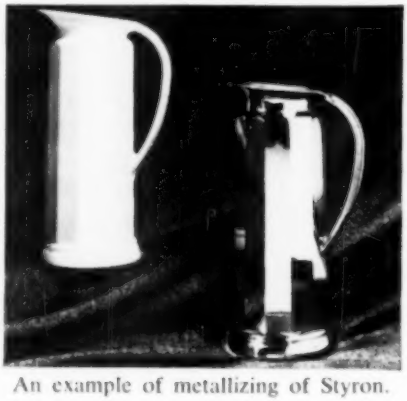
A look at the many Styron® products on the market shows the beauty in design, wide color range and varying physical properties possible with Dow's different Styron formulations. There may be times, however, when you want to add further functional and decorative effects to Styron. Coatings and finishes can be the perfect answer if they're selected and used properly.

Dow has studied and evaluated many techniques and methods for the coating of Styron. The very *business* of Plastiatrics is to provide a continuous study, analysis and treatment of all phases of plastics and their application so that successful results can be predicted.

Which Coating or Finish Do You Use?

First, of course, you consider the results you want. Then you apply the coating that will give you that result. It is important, however, to pay careful attention to application technique. For example, lacquers are used for the striking back painting method which gives depth and gem-like appearance to escutcheons. But to be just right, the molded part should be checked for residual stress and the effect of lacquer solvent on crazing. There must be enough solvent attack to insure lasting adhesion but not enough to weaken the molding. It is important to know whether lubricants are used in the material or molding process and what effect they may have on surface adhesion. Surface treatment and application techniques can be worked out to meet specific needs. For instance, metallizing can be accomplished in any one of five different ways to provide additional physical properties or improved appearance.



An example of metallizing of Styron.

For a specialized interior finish to resemble suede, felt or mohair, a flocking process may be used. Metallizing, flocking and lacquering require many of the same basic considerations when it comes to proper application techniques. Printing and labeling to add printed words, identification, directions, etc., also pose application problems similar to lacquering, although not as critical in nature. Of course, every different type of application technique, coating, or finish requires special attention to its own problems.

How Dow's Plastiatrics Studies Can Help You

Dow studies can be of service to you because they include extensive information not only on different types of coatings and finishes but on proper application techniques, surface cleaners, drying procedures and other data. This information can help you produce results while avoiding a long trial-and-error approach that often means a costly expenditure in both time and money.

Free Bulletin Offered

For more complete information on this subject, send for your free copy of Dow's Technical Service bulletin entitled "Coatings and Finishes for Styron." Write to Plastics Sales Department PL. 455Z-2, THE DOW CHEMICAL COMPANY, Midland, Michigan.

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Rheology Issue

This issue of the Journal has papers dealing with the single topic of rheology. It is frankly an experiment, which might be repeated annually if there is enough demand for it. In this issue we also strayed afield in one article from plastics, in the hope that there is something to learn in allied fields. We invite your comments on the notion of an annual rheology issue.

When the Society of Rheology convened for its Annual Meeting in November, 1954, at the National Bureau of Standards in Washington, the turnout of papers and the attendance were certainly assisted by the attractions offered by the meeting place, today's most important center of fundamental rheological research in the country; but at the same time it was a sign of the increasing awareness of Science and Technology of what Rheology, the molecular-physical-mathematical approach to the deformation of matter, had to offer.

The large number of papers given presented our Society with a serious publication problem. Traditionally, these papers are collected in a Rheology Issue of a host-Journal, for some time the Journal of Colloid Science and for the last two years the Journal of Applied Physics. This year, the printing space required definitely exceeded even the most generous allowance that any one journal could grant in fairness to its other contributors. We were therefore most fortunate in obtaining additional space in the Society of Plastics Engineers Journal, so that we will be able to distribute two Rheology Issues this year.

Notwithstanding this favorable development, the absence of a single comprehensive volume will be regretted by some. In order to maintain the coherence of the meeting to a certain extent, the Editor has kindly agreed to print below the titles of the papers which will appear in the companion issue of the Journal of Applied Physics, as well as of those authors who were under different publication obligations. Abstracts of all papers have appeared collectively in the Fall Issue of the Rheology Bulletin, 1954.

Papers which will appear in the Rheology Issue of the Journal of Applied Physics, July 1955:

"Viscosity and Elasticity of Oil Surfaces and Oil-Water Interfaces"
Dean W. Criddle and Arthur L. Meader
California Research Corporation, Richmond, Cal.

"Application of the Superposition Principle and Theories of Mechanical Equation of State, Strain- and Time-Hardening to Creep of Plastics under Changing Loads"
W. N. Findley and Gautum Khosla, Brown University, Providence, R. I.

"Dynamic Mechanical Properties of Non-Polymeric Liquids
T. Litovitz and E. Carnevale
Naval Ordnance Laboratory, Washington, D. C.

"Diffusion Controlled Stress-Relaxation"
R. H. McMickle and E. T. Kubu
B. F. Goodrich Research Center, Brecksville, Ohio

"Contribution to the Study of Transport Phenomena in Gases at High Densities"
A. Michels, University of Maryland, College Park, Md. and A. S. Friedman, National Bureau of Standards, Washington, D. C.

"Preliminary Results in Measuring Dynamic Compressibilities"
W. Philippoff and J. Brodnyan, Franklin Institute, Phila., Pa.

"Theory of Non-Newtonian Flow. I. Solid Plastic System"
Taikyue Ree and Henry Eyring, University of Utah, Salt Lake City

"Theory of Non-Newtonian Flow. II. Solution System of High Polymers"
Taikyue Ree and Henry Eyring, University of Utah

"The Effect of Molecular Weight on the Creep Behavior of Cellulose Acetate Films"
D. L. Swanson, American Cyanamid Co., Stamford, Conn.
J. W. Williams, University of Wisconsin, Madison, Wis.

"Elastic Moduli as a Measure of Anistropy in Nylon and Dacron Fibers"
J. H. Wakelin, et al, Textile Research Institute, Princeton, N. J.

Papers which will appear elsewhere:

"Determination of Baking Values of Bread Doughs by Rheological Measurements"
C. W. Brabender and Stephen J. Loska, Jr., Pillsbury Mills, Inc., Minneapolis, Minn.

"Concentration Dependence of the Rheology Behavior of the Polyisobutylene-Decalin System"
T. W. De Witt, et al, Mellon Institute, Pittsburgh, Pa.

"Acoustics and the Liquid State"
R. B. Lindsay, Brown University, Providence, R. I.

"Rheology of Bituminous Pavements"
C. Mack, Imperial Oil, Ltd., Sarnia, Ontario

"A Cone and Plate Viscometer"
H. Markovitz, et al, Mellon Institute, Pittsburgh, Pa.

"Stress-Strain Relationships in Yarns when Strained at Impact Velocities Ranging from 10 to 100 meters per second"
Herbert F. Shiefer, National Bureau of Standards, Washington, D. C.

Papers which will not be published:

"Visco-Elastic Density Changes in Glass"
O. L. Anderson, Bell Telephone Laboratories, Murray Hill, N. J.

"The Statistical Mechanics of Viscosity"
Melville S. Green, National Bureau of Standards, Washington, D. C.

"Non-Linear Visco-Elastic Behavior in Shear of Rubber"
Herbert Leaderman, National Bureau of Standards, Washington, D. C.

"Some Remarks on the Theory of Non-Linear Visco-Elasticity"
R. S. Rivlin, Brown University, Providence, R. I.

F. R. Eirich, Editor
The Society of Rheology

Dynamic Creep of Plastics

Joseph Marin and W. J. Schuman
Pennsylvania State University

SUMMARY

A theory is presented in this paper for predicting creep in the case of fluctuating stress based upon an assumed creep-stress-time relation for simple static tension. The condition of dynamic stress considered is that of a completely reversed axial stress superimposed on a static axial tensile stress.

A second part of this paper presents both static and dynamic tension creep test results for a plastic designated as Plexiglas 1A. A comparsion of the theoretical dynamic creep-time relations based upon the static creep results agree approximately with the actual dynamic tension creep-time relations.

INTRODUCTION

Most available creep data for plastics is for static stress conditions. However, many applications of plastics include dynamic stress applications in which the stresses are not statically applied. The few investigations on dynamic creep that have been conducted have not included a theoretical interpretation of the test results. (1 to 5)*

The test results included herein were also reported in a paper presented at the 1954 U. S. National Congress on Applied Mechanics (6). However, the interpretation of these results is new.

PROPOSED THEORY FOR DYNAMIC CREEP

In order to develop a theoretical relation for dynamic tensile creep stresses, a relation for static tension creep strain will be selected, made up of the parts as indicated in Fig. 1, and based upon the following assumptions:

(1) The total strain of the material at a time t and constant temperature is composed of four component parts; the elastic strain, the initial plastic strain, the transient creep strain and the minimum creep-rate strain. These strains are shown in Fig. 1 for static stress. The subdivision of the strains is in keeping with considerations described by Orowan (7) and others, and can be represented by a model using springs and a dash pot.

(2) The elastic strain is proportional to the instantaneous stress or

$$\varepsilon_j = \sigma / E \qquad (1)$$

where ε_j = the elastic strain
σ = the instantaneous stress
E = Young's Modulus of Elasticity

(3) The initial plastic strain, for stresses greater than the proportional limit, takes place during the first loading cycle. This initial plastic strain will be assumed to be the following function of the maximum stress.

$$\varepsilon_b = k\,(\sigma\ max / E)^B \qquad (2)$$

where ε_b = the initial strain
$\sigma\ max$ = The maximum value of stress for stresses greater than the proportional limit
k and B = Experimental constants obtained from static stress-creep tests.

After the initial plastic strain, the material will behave elastically. Considerable test data for various materials indicates that eq. (2) is a good approximation.

(4) The transient creep strain is assumed related to the instantaneous stress by the condition that the transient creep rate is a function of the instantaneous stress and the transient creep strain, or

$$d\,\acute{\varepsilon}_b / dt = q\,(K\,\sigma^A - \acute{\varepsilon}_b) \qquad (3)$$

where $d\acute{\varepsilon}_b / dt$ = the transient creep rate
$\acute{\varepsilon}$ = the transient creep strain
q, K, A = experimental constants based on static stress-creep tests.

It is also assumed that the foregoing transient creep strain is fully recoverable. Equation (3) is used for expressing the transient creep since it gives a good representation for the early stage of creep.

(5) The minimum creep rate strain is assumed related to the instantaneous stress only, as given by the

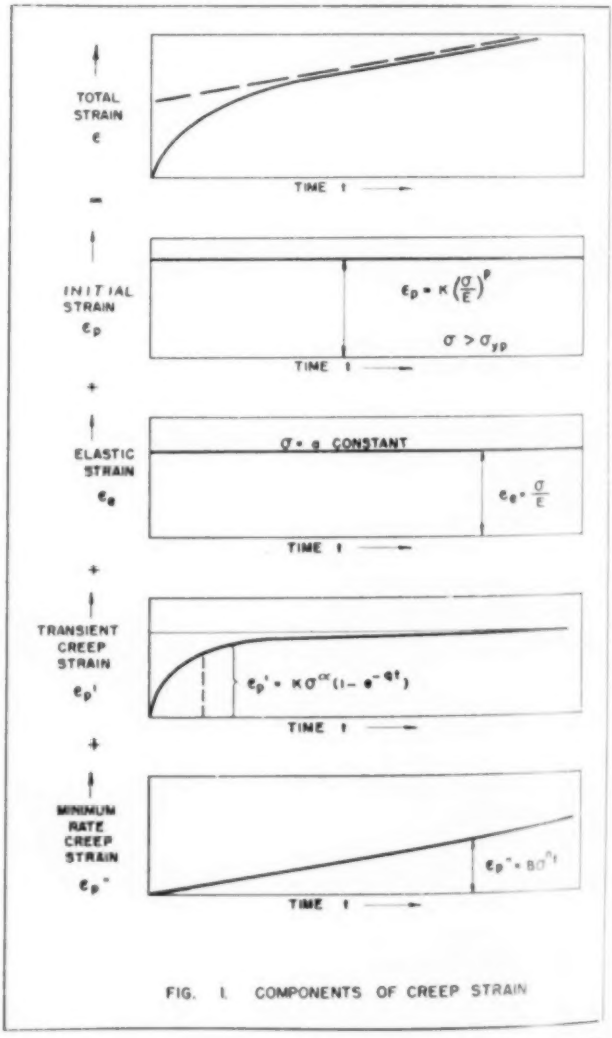

FIG. 1. COMPONENTS OF CREEP STRAIN

Numbers indicate references given in the Bibliography at the end of this paper.

following relation.

$$d\,\ddot{\varepsilon}_b / dt = C\,\sigma^n \qquad (4)$$

where $d\ddot{\varepsilon}_b/dt$ = the minimum constant creep rate

$\ddot{\varepsilon}_b$ = the minimum creep rate strain

C, w = experimental constants based on static tension creep data.

For many materials, eq. (4) has been found to be a good approximation. From equations (1) to (4), the total strain ε at a time **t** is

$$\varepsilon = \varepsilon_j + \varepsilon_b + \acute{\varepsilon}_b + \ddot{\varepsilon}_b \qquad (5)$$

$$\text{or } \varepsilon = \sigma / E + k\,(\sigma\, max / E)^B + \int_o^t (d\acute{\varepsilon}_b / dt)\,dt + \int_o^t (d\ddot{\varepsilon}_b / dt)\,dt$$

$$\text{or } \varepsilon = \sigma / E + k\,(\sigma\, max / E)^B + \int_o^t q\,(K\,\sigma^A - \acute{\varepsilon}_b)\,dt + \int_o^t C\,\sigma^n\,dt$$

It is desirable to represent the strain ε as a function of the stress σ. In eq. (5), the variable strain $\acute{\varepsilon}_b$ appears. The strain $\acute{\varepsilon}_b$ can be expressed in terms of the stress σ by using eq. (3). By solving eq. (3) and using the boundary condition that $\acute{\varepsilon}_b = 0$ when $t = 0$,

$$\acute{\varepsilon}_b = e^{-qt} \int_o^t e^{qt}\, qk\, \sigma^A\, dt \qquad (6)$$

Replacing $\acute{\varepsilon}_b$ in eq. (5) by the value from eq. (6),

$$\varepsilon = \sigma / E + k\,(\sigma\, max / E)^B + e^{-qt} \int_o^t e^{qt}\, q\, K\, \sigma^A\, dt + \int_o^t C\,\sigma^n\,dt \qquad (7)$$

Equation (7) represents the variation in strain with stress in terms of static creep constants. This equation will now be used to determine the creep strain-stress values for dynamic axial stress as represented in Fig. 2 and defined by the equation,

$$\sigma = \sigma m + \bar{\sigma} \sin \omega t \qquad (8)$$

where σ = the instantaneous stress

σm = the means or static preload stress

$\bar{\sigma}$ = the fluctuating superimposed stress

ω = the frequency of application of the fluctuating stress.

Placing values of σ from eq. (8) in eq. (7),

$$\varepsilon = 1 / E\,(\sigma m + \bar{\sigma} \sin \omega t) + k\,[(\sigma m + \bar{\sigma}) / E)^B + \int_o^t e^{qt}\, q\, K\, (\sigma m + \bar{\sigma} \sin \omega t)^A\, dt + \int_o^t C\,(\sigma m + \bar{\sigma} \sin \omega t)^n\, d$$

If A and **n** in eq. (9) are not integers — the case usually encountered — then closed solutions cannot be found. A solution can then be made by interpolating for the values of strain using the solutions for the nearest integer values of **A** and **n**. A more accurate method is to use a semi-graphical procedure for solving the two integrals in eq. (9). For this purpose, the first integral in eq. (9), representing the transient creep $\acute{\varepsilon}_b$, can be written,

$$\acute{\varepsilon}_b / \sigma^A m = e^{-qt} \int_o^t e^{qt}\, q\, K\, (1 + R \sin \omega t)^A\, dt \qquad (10)$$

where $R = \bar{\sigma} / \sigma m$

Graphical integration of eq. (10) can now be made by evaluating the integral of eq. (10) for various integer values of A, stress ratios **R**, and time **t**. In this way, sets of curves as shown in Fig. 3 can be obtained for values of A = 1, 2, 3 and 4 as indicated for each set of curves. Using Fig. 3, a set of graphs, as given in Fig. 4 can be drawn relating $\acute{\varepsilon}_b / \sigma^A m$ versus A for various values of **t** and **R**. Fig. 4 can then be used to find the value of $\acute{\varepsilon}_b / \sigma^A m$ for the value of A, time **t** and stress ratio **R**. The transient creep can now be obtained by multiplying the value selected from Fig. 4 by σm.

The values of the second integral in eq. (9) can be obtained in a manner similar to that described for the first integral. With the foregoing integrals evaluated, the total strain ε can now be found using eq. (9). It should be noted that the foregoing theory assumes that a dynamic stress does not change the material properties.

COMPARISON OF THEORY WITH TESTS

Static and dynamic creep tests were conducted to check the validity of the foregoing theory. These test results have been previously reported (6) so that only a brief summary will be given here.

The material tested was a commercial grade of polymethylmethacrylate manufactured by Rohm and Haas Company and designated as Plexiglas 1A. The manufacturer lists the following properties for this material: tensile strength 6,000 to 10,000 psi, Modulus of Elasticity in tension 3 to 5 $\times$ 10^5 psi and percentage elongation 1 to 15%.

The tests were conducted in a creep laboratory with controlled temperature and humidity. The temperature was maintained at 73 $\pm$ 2 degrees F° and the humidity at 50 $\pm$ 2 percent relative humidity.

The specimens for both the static and dynamic tests had an overall length of 8 in. with a reduced test section of 3½ in. long and ½ in. × ¼ in. in cross-section. The constant static creep tests were conducted in the creep tension machine shown in Fig. 5. A photograph of the dynamic creep testing machine is shown in Fig. 6. A schematic drawing of the machine in Fig. 6 is shown in Fig. 7. Referring to Fig. 7, the specimen **S** is subjected to a mean stress by means of the lever **L** and the dead weight **W**. A constant speed, 1740 rpm, three phase induction motor drives the eccentric **E** which causes the lower end of the spring **SP** to move up and down in simple harmonic motion. This motion produces a fluctuating axial load on the specimen. A dynamometer **D** in series with the specimen is used to measure the fluctuating load. The dynamometer consists essentially of an aluminum bar with SR-4 strain gages bonded to it, calibrated to measure axial load. The creep strains in the specimens were measured by SR-4 gages attached to the specimens.

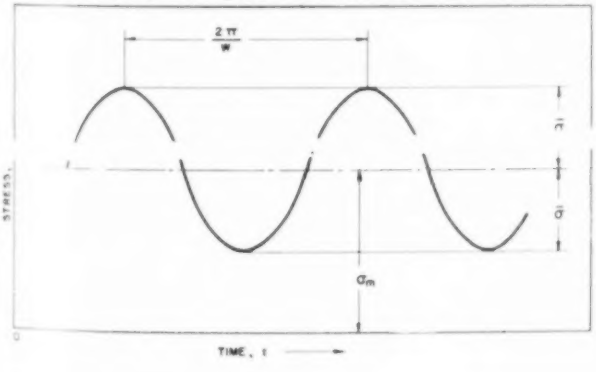

FIG. 2 VARIATION OF STRESS WITH TIME

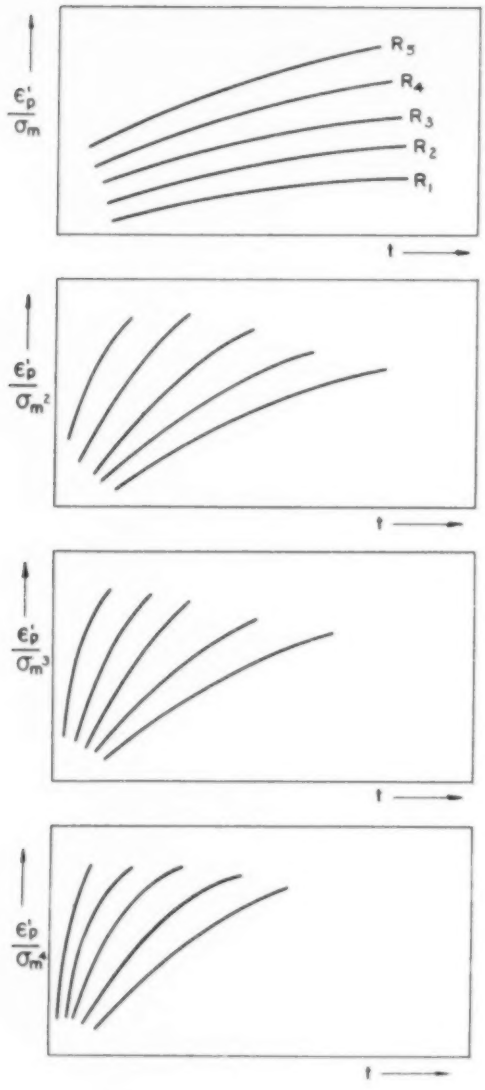

FIG. 3. $\frac{\epsilon_p'}{\sigma_m^{\alpha}}$ vs. t (FOR α = 1,2,3,4)

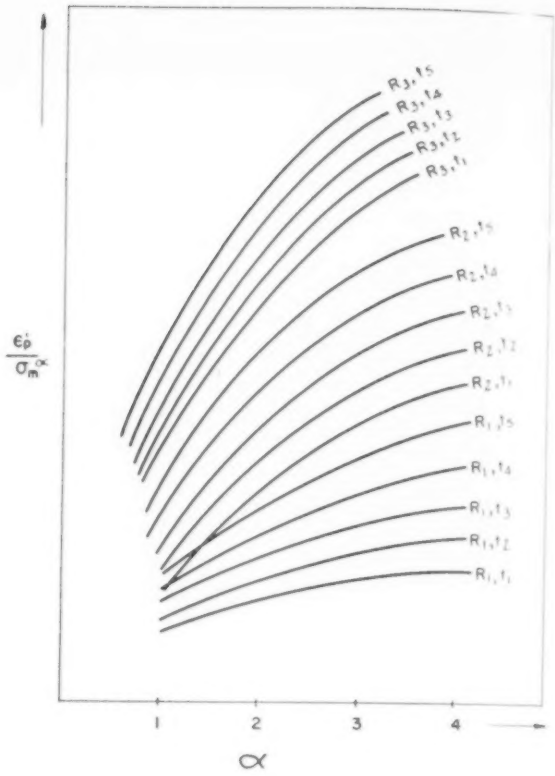

FIG. 4 $\frac{\epsilon_p'}{\sigma_m^{\alpha}}$ vs. α

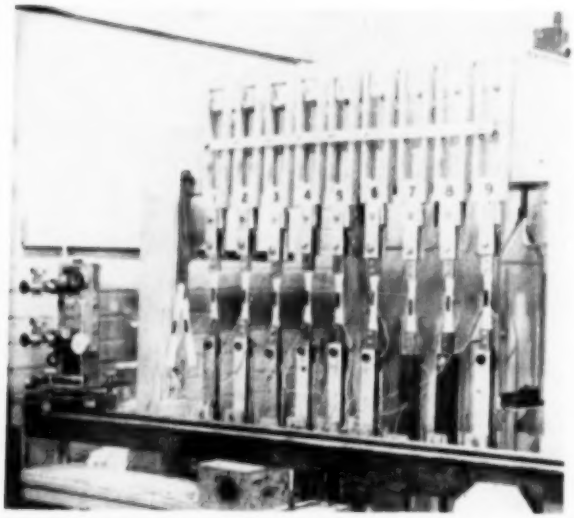

Figure 5. Photograph of the tension creep testing machine.

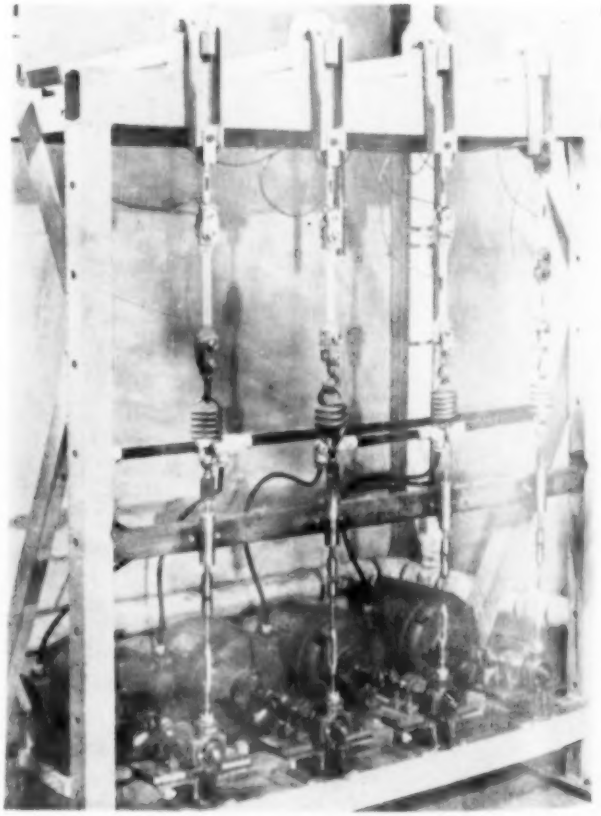

Figure 6. Photograph of the dynamic creep testing machine.

Fig. 6 shows a Hathaway Oscillograph with auxiliary equipment which was also used as a check for the measurement of both load and creep strain. These tests were conducted so that the mean loads remained constant as the specimens elongated.

The static creep-time relations, using the machine in Fig. 5, are given in Fig. 8. The theoretical creep-time relations, based on eq. (7), are also shown in Fig. 8.

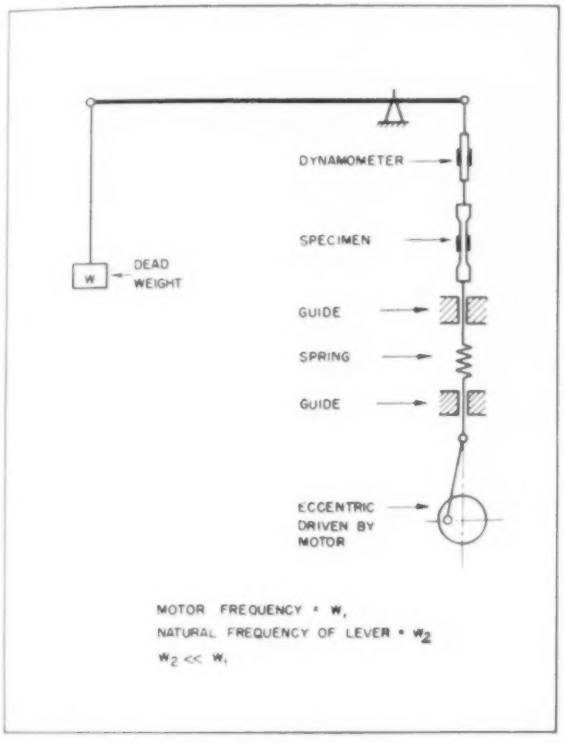

FIG 7 SCHEMATIC ILLUSTRATION OF ONE UNIT OF THE DYNAMIC CREEP TESTING MACHINE

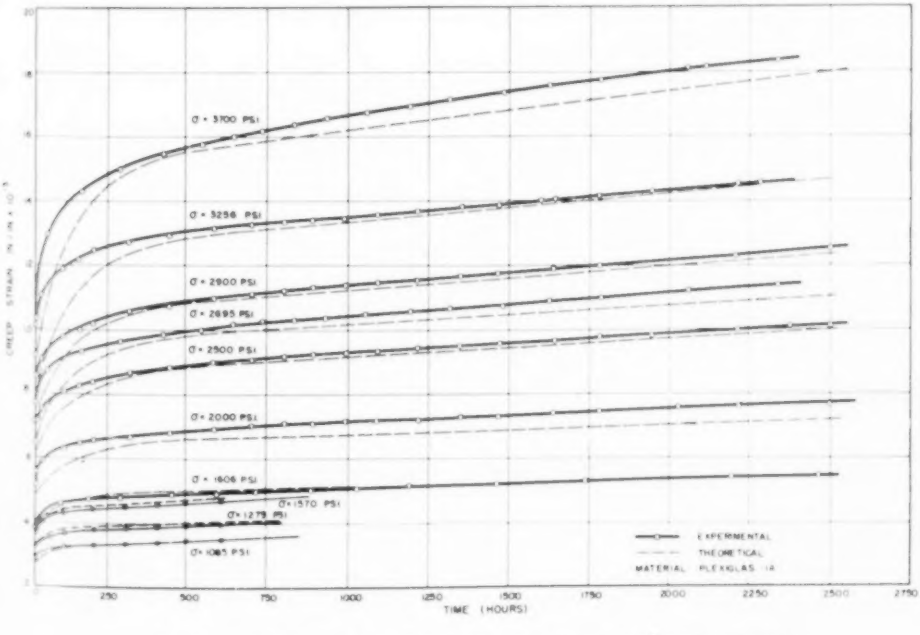

FIG 8 REPRESENTATION OF TENSION CREEP DATA BY EXPRESSION $\epsilon = \frac{\sigma}{E} + K\sigma^m(1-e^{-qt}) + B\sigma^n t$

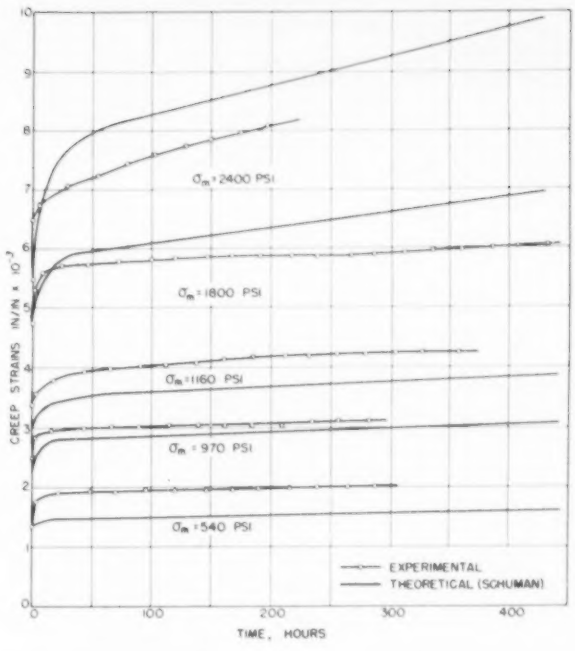

FIG 9 COMPARISON OF ACTUAL AND THEORETICAL DYNAMIC CREEP STRAINS

The experimental constants in eq. (7) were obtained by fitting the transient and constant creep rate strains using the method of least squares. The values of the constants obtained are: $E = 3.90 \times 10^5$, $K = 1.09 \times 10^{19}$, $B = 1.30 \times 10^{-15}$, $q = 0.14$, $A = 2.17$, and $n = 2.25$.

The dynamic creep test results are shown in Fig. 9. The five specimens tested were subjected to mean stresses as shown. The ratios of the variable to mean stress were 0.31, 0.17, 0.15, 0.09, 0.07 corresponding to mean stresses of 540, 970, 1160, 1800 and 2400 psi respectively.

Using the static tension creep constant based on Fig. 8 and eq. (7), the dynamic creep relations were obtained by the semigraphical method described in this paper. The resulting theoretical relations are shown in Fig. 9 by the dotted lines. A comparison of the theoretical and actual creep time relations in Fig. 9 shows approximate agreement between test results and theory.

CONCLUSIONS

A theory is developed in this paper for predicting dynamic creep-time relations based upon static creep values. Some preliminary test results are given to show that theory proposed can be used to predict creep strains under dynamic stress for the particular material, frequency and load range used.

(Please turn to page 54)

Effect of Fillers on the Dynamical Mechanical Properties of Polystyrene

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and Paul G. Richmond

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INTRODUCTION

The effects of reinforcing fillers such as carbon black in changing the mechanical properties of rubbers are well known and have been intensively studied. Much less is known, however, about the properties of rigid polymers containing fillers. Therefore, an investigation was carried out on certain mechanical properties of polystyrene containing fillers of different sizes, shapes, and mechanical behavior. The mechanical properties investigated were the dynamic shear modulus and damping. The measurements were made with a recording torsion pendulum at a frequency of about one cycle per second; this instrument has been described elsewhere (1). The damping is calculated as the logarithmic decrement from the curve of the damped oscillations. The shear modulus is calculated from the moment of inertia and frequency of the oscillating system and the dimensions of the test specimen.

EXPERIMENTAL RESULTS

The fillers were incorporated in the polystyrene by mixing on a mill roll. Test specimens (4" x ⅜" x 0.025") were pressed out between polished plates with a 0.025 inch spacer at 160°C and then cooled. Tests were carried out over the range 20°C to 105°C. Ground calcium carbonate, ground mica, powdered and fibrous asbestos, glass fibers, cotton cloth fibers and wood flour were used as fillers. The characteristics and concentrations of fillers used are listed in Table I. It was noted that the length of the glass and asbestos fibers was greatly reduced by the mill rolling.

The experimental results are shown in Figures 1, 2, and 3 and in Table II. In general, the fillers increase the modulus over that of unfilled polystyrene, and they decrease the damping. The increase in modulus appears to be approximately proportional to the volume fraction of filler, the proportionality factor being dependent upon the type of filler. The powdered mica was outstanding in its reinforcing action while the wood flour had little effect on the modulus. The damping (logarithmic decrement) is a measure of the ratio of energy dissipated as heat to the recoverable energy stored in the specimen during a mechanical deformation. All the fillers except the glass fibers and cotton fibers decreased the damping at 30°C. Near 100°C polystyrene changes from a

TABLE I
Concentration and Description of Fillers Studied

Filler	Weight Per Cent	Density	Shear Mod. (Dynes/cm²)	Particle Size (cm)	Particle Shape
Mica	20 40 60	2.80	2.6 x 10^{11}	5 x 10^{-3} (diameter)	Flat Plates
Asbestos (Powder)	20 40 60	2.40	6.9 x 10^{11}	10^{-2}	Heterogeneous powder
Calcium Carbonate	8 14 20	2.70	2.3 x 10^{11}	10^{-3}	Rough Spheres
Wood Flour (Yellow pine)	5 10 15	0.50	0.95 x 10^{11}	10^{-2} length	Fibers
Glass Fibers	16	2.40	2.8 x 10^{11}	<0.1 10^{-3} (dia.)	Rods
Glass Fibers	19.9 22.3 18.3	2.40	2.8 x 10^{11}	1/16" orig. 1/8" orig. 1/4" orig.	Rods
Asbestos Fibers	40	2.40	6.9 x 10^{11}	0.1	Fibers with agglomerates
Cotton Cloth	20	1.48	2.8 x 10^{11}	5 x 10^{-2} length	Fibers

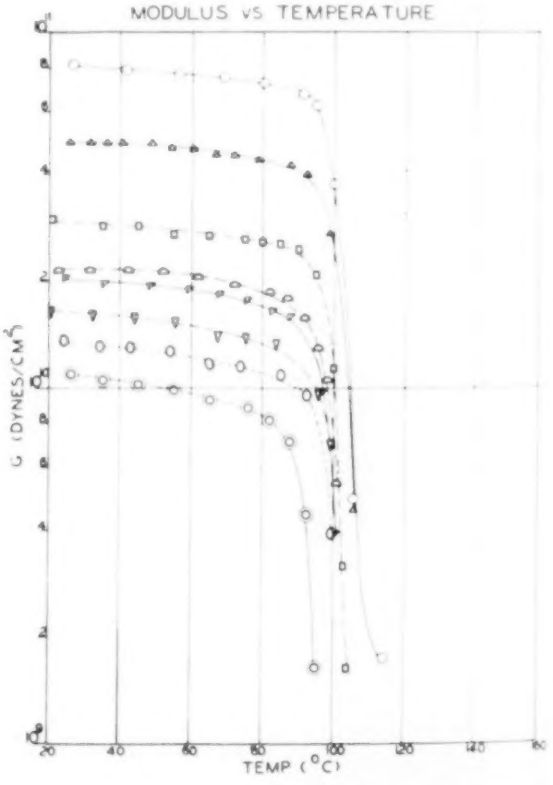

Fig. 1. Shear Modulus vs. temperature for filled polystyrene.

◎ Polystyrene control.
△ 40% asbestos.
▽ 20% mica.
○ 60% mica.
△ 40% mica.
▽ 20% asbestos.
0 20% calcium carbonate.
□ 60% asbestos.

TABLE II

Filler	Volume Per Cent	Modulus G (Dynes/cm²)	Logarithmic Decrements δ	Gδ	Adhesion Factor A
Mica	8.65	2.00×10^{10}	0.045	9.00×10^{8}	
	20.2	4.92	0.046	2.26×10^{9}	0.89
	36.3	8.00	0.033	2.65×10^{9}	
Asbestos Powder	9.98	1.63×10^{10}	0.048	7.80×10^{8}	
	22.8	2.13	0.036	7.70×10^{8}	0.086
	40.0	2.96	0.034	1.01×10^{9}	
Asbestos Fibers	22.8	1.95×10^{10}	0.054	1.32×10^{9}	0.074
Calcium Carbonate	3.30	1.15×10^{10}	0.058	6.67×10^{8}	
	6.03	1.27	0.044	5.60×10^{8}	0.20
	9.00	1.42	0.053	7.53×10^{8}	
Wood Flour	10.1	1.00×10^{10}	0.050	5.00×10^{8}	
	19.1	1.28	0.049	6.27×10^{8}	0.14 (?)
	27.3	1.15	0.053	6.08×10^{8}	
Glass Fibers	7.76	1.28×10^{10}	0.115	1.70×10^{9}	0.15
	18.3	1.64	0.080	1.31×10^{9}	0.15
	19.9	2.10	0.075	1.58×10^{9}	0.23
	22.3	1.96	0.078	1.53×10^{9}	0.17
Cotton Fibers	15.2	1.45×10^{10}	0.074	1.09×10^{9}	0.13
Unfilled Polystyrene		1.07×10^{10}	0.060	6.42×10^{8}	

rigid material to a rubbery one; in this temperature range the damping goes through a maximum. Rigid fillers shift this damping peak to slightly higher temperatures. The product of the modulus and the damping gives a quantity, Gδ, which is proportional to the heat generated in a material by a unit deformation. As shown in Table II, glass fibers, asbestos, cotton fibers, and mica cause more heat to be produced than in unfilled polystyrene while wood flour and calcium carbonate have little effect.

DISCUSSION

It is to be expected that the effect of a filler on the modulus should be proportional to its volume fraction, and as an approximation should obey the equation

$$G = G_1 \phi_1 + AG_2 \phi_2$$

or

$$G = G_1 \left[1 + (AG_2/G_1 - 1) \phi_2 \right]$$

since $\phi_1 + \phi_2 = 1$. In these equations G is the shear modulus and ϕ is the volume fraction; the subscripts 1 and 2 refer to polystyrene and filler, respectively. The quantity A is an "adhesion factor" which normally can vary between the limits zero and one. From elasticity theory it can be shown that for fillers with modulus considerably greater than polystyrene, A is somewhat less than 1.0 even if there is perfect adhesion between the polystyrene and the filler. A can be negative in cases where the filler has an apparent density in the plastic less than the density measured in bulk form; air entrapped with the filler can bring about such an effect.

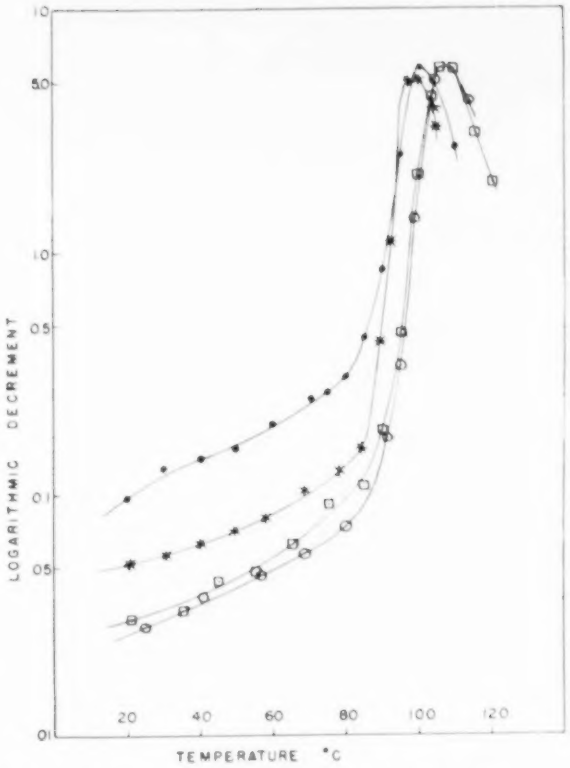

Fig. 2. Damping vs. temperature for filled polystyrenes.

✱ Polystyrene control. ⊡ 60% asbestos.
● 16% glass fibers. ⊙ 60% mica.

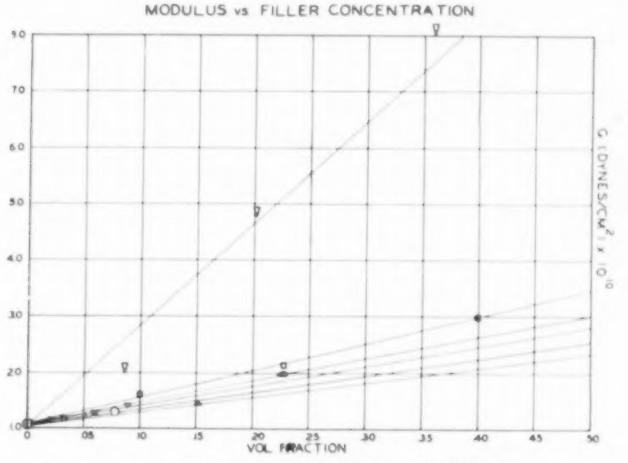

Fig. 3. Shear modulus vs. volume fraction of filler.

▽ mica, ▽ calcium carbonate,
□ asbestos, ⊙ glass fibers,
asbestos fibers, △ cloth fibers.

Figure 3 indicates that the shear modulus is approximately a linear function of the volume fraction up to at least 40 per cent. Knowing the shear modulus of the pure filler, it is then possible to calculate values of the "adhesion factor" A for the different fillers. The A values at 30°C are listed in Table II. These values may not be quite correct in all cases since the shear modulus of some of the fillers is not accurately known;

(Please turn to page 46)

Temperature, Velocity and Viscosity Distribution For a Temperature Sensitive Viscous Liquid

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ABSTRACT

A procedure is presented for computing the temperature, velocity, and viscosity distributions across a stream of a temperature sensitive viscous liquid in laminar flow when being sheared between two plane parallel plates. A general solution is given for a liquid the viscosity of which obeys an empirical logarithmic temperature dependence.

The use of the procedure for correcting experimental viscosity measurements is described using published data.

Application of the procedure to the design and operation of plastic processing apparatus is mentioned.

INTRODUCTION:

When a molten plastic material or other highly viscous substance is caused to flow mechanical energy is degraded into heat and the material temperature rises. If the rate of heat generation is sufficiently small the system can be maintained at essentially isothermal conditions by conducting the generated heat into the flow containing boundaries which act as a heat reservoir. However, often it is not possible to remove the generated heat until a substantial rise in temperature has been established and the system is not isothermal. This rise in temperature has been neglected by some investigators.

It is of interest to study the general case where temperature variations across the liquid stream are appreciable and to determine the temperature and velocity distributions. Such a study will facilitate a more adequate interpretation of flow curves and permit one to determine whether or not the temperature effects are sufficient to cause significant changes in the thermal, viscous, or chemical properties of the fluid.

There has been much discussion in the literature on this subject and in particular the work of Weltmann and Kuhns (9) is cited. They present a method for estimating the temperature distribution across the gap of a rotation viscometer and discuss the role of thixotropy and temperature in causing the decrease of viscosity when the material is subjected to shearing forces. When the thermal effects are ignored even a "Newtonian" liquid may be observed to have marked thixotropy and the flow curve may be interpreted as "non-Newtonian". Most plastic melts are considered to be non-Newtonian, the viscosity coefficient varying with shearing stress or rate of shear. Data obtained at low shear rates (less than about 4 sec.$^{-1}$) tend to confirm this conclusion. However, when one is concerned with the flow characteristics of these materials at rates of shear several orders of magnitude higher the data obtained at low shear rates are often unsatisfactory.

Goodeve and Whitfield (3) have presented arguments for conducting rotation viscometry tests at equilibrium torque, a procedure which is essential if one is to apply the results presented in this paper. Mill and Gates (5) studied the temperature effects in a rotation viscometer assuming that the rate of shear at the bob was not altered by the temperature rise. They obtained a corrected torque value by multiplying the torque at the base temperature by the ratio of the viscosity at the observed temperature divided by the viscosity at the base temperature. Merrill (4) described a coaxial cylinder viscometer for the study of liquids under high velocity gradients and discussed thermal effects. He installed a thermocouple in the liquid and measured the temperature rise showing it to be negligible. His conclusion is shown to be valid using the equations derived in this paper. Literature references on temperature effects in rotation viscometry have been summarized by Blok (1).

Teichmann (8) considered the case of viscous flow between plates and employed an average value for the fluid viscosity. Wilson and Mitchell (10) studied the self-induced temperature effects and their influence on the laminar flow of liquids. They considered the unsteady state case assuming the material was uniform across the flow path and varied with distance along the flow path. Brinkman (2) studied the heat effects in capillary flow and was concerned with the unsteady state case.

The procedure used in this paper was suggested by Prager and Rivlin (7) and is similar to a calculation that has been carried out by Nahme (6). Data of Weltmann and Kuhns (9) are used to provide a numerical comparison with their method. The system considered in this paper consists of two parallel plates which is a reasonable approximation for rotation viscometers when the ratio of the bob radius to the cup radius is near unity. The system also approximates the conventional smooth surfaced mixing torpedo at the end of an extrusion screw if the rotational velocity is large compared to the velocity along the torpedo. The method can be used to estimate the limiting temperature distribution across the extruder screw channel if the channel depth is less than about one tenth the width and the relative velocity of the screw surface with respect to the barrel surface is assumed to be along the channel axis.

SYSTEM AND LIMITING CONDITIONS:

Consider a system comprised of two rigid parallel plates whose length and width are large compared with the distance between them. A viscous liquid is contained between the plates and is caused to flow because of the steady motion of one of the plates along the reference axis in its plane. Each plate is maintained at a constant temperature, but the two plates are not necessarily at the same temperature. A steady state exists which is equivalent to stating that the point values of temperature, velocity, and viscosity depend only on the position across the stream, that is the Z coordinate. Laminar flow is presumed.

The particular viscosity temperature dependence law selected should provide adequate correlation over the range of temperatures existing in the system. For this reason the empirical exponential law given in eq. (1) is chosen. If the shear stress dependence of viscosity is known it can be incorporated into the equations since the shear stress is constant for a given dimensional set

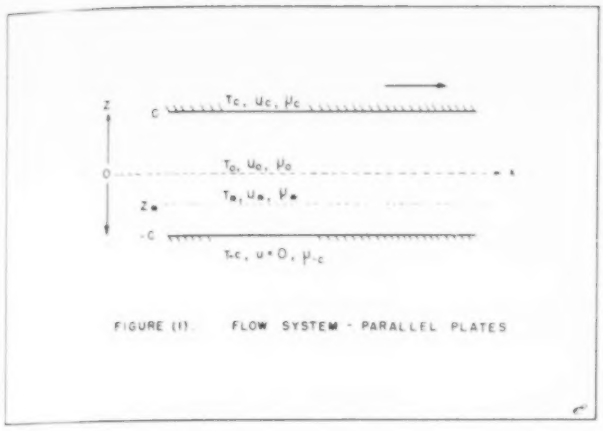


FIGURE (1). FLOW SYSTEM - PARALLEL PLATES

up of the plane parallel plate geometry and for a given mode of operation.

The system is pictured schematically in figure 1. The limiting conditions are:

(1)	(2)	(3)	(4)
$Z = C$	$Z = 0$	$Z = Z^*$	$Z = -C$
$T = T(c)$	$T = T_o$	$T = T^*$	$T = T(-c)$
$u = u(c)$	$u = u_*$	$u = u^*$	$u = 0$
$\mu = \mu(c)$	$\mu = \mu_*$	$\mu = \mu^*$	$\mu = \mu(-c)$

$$dT/dZ = 0$$

When the two plates are at the same temperature, $T(-c) = T(c)$, and $Z^* = 0$, because of the symmetry of the system. For convenience the value of $T(-c)$ is taken to be equal or greater than $T(c)$ which locates Z^* between position -C and 0.

The basic equations are:

$$\mu = \mu' e^{-AT} \qquad (1)$$

where μ is viscosity, T is temperature, and μ' and A are constants;

$$du / dZ = S / \mu \qquad (2)$$

where du/dZ is the velocity gradient and S is the shearing stress;

$$S\, du / dZ = -J K d^2 T / dZ^2 \qquad (3)$$

where K is the thermal conductivity of the liquid and J is a factor for converting thermal units into mechanical units. Equation (3) is the differential equation stating the equality of heat flow from the system and mechanical energy dissipation within the system. At steady state all of the heat generated in the liquid by viscous shearing must be conducted out from the system.

TEMPERATURE DISTRIBUTION

Combining equations (1) and (2) and substituting in equation (3) one obtains,

$$d^2 T / dZ^2 = -2\lambda^2 e^{AT} / A \qquad (4)$$

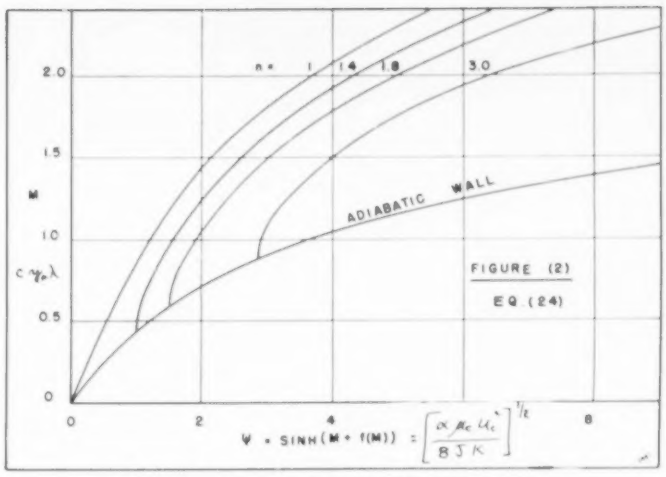
where $\lambda = (A S^2 / 2 J K \mu')^{1/2}$

Equation (4) can be reduced to a first order equation and the variables separated if a new variable, $p = dT/dZ$, is defined. After integrating the result is,

$$(dT/dZ)^2 = -4\lambda^2 e^{AT} / A^2 + I \qquad (5)$$

The constant of integration I is evaluated using limiting condition (3) and equation (5) becomes,

$$(dT/dZ) = (\pm 2\lambda / A)(e^{AT^*} - e^{AT})^{1/2} \qquad (6)$$



(Alpha in the equations in the figure above is written as A in the text.)

The temperature gradient is positive when $-C \leq Z \leq Z^*$ and negative when $Z^* \leq Z \leq C$.

Equation (6) is solved by integration using the limits -C to Z and $T(-c)$ to T when (dT/dZ) is positive and Z^* to Z and T^* to $T(c)$ when (1) is negative. Using the notation,

$$y = e^{AT/2} \quad \text{and} \quad y^* = e^{AT^*/2}$$

the solution is,

$$y = y^* / \cosh [\cosh^{-1} y^* / y(-c) - (Z + C) y^* \lambda] \qquad (7)$$

when $dT/dZ \geq 0$

$$y = y^* / \cosh [(Z - Z^*) y^* \lambda] \qquad (8)$$

when $dT/dZ \leq 0$

An expression relating y^* and Z^* can be obtained from equation (7) when the values of $y = y^*$ and $Z = Z^*$ are substituted. The resulting expression is,

$$y^* = y(-c) \cosh [(Z^* + C) y^* \lambda] \qquad (9)$$

It is convenient to define three parameters for ease of numerical computation. These are,

$$n = y(-c) / y(c) \qquad n \leq 1 \qquad (10)$$

$$M = C y^* \lambda \qquad (11)$$

$$f(M) = -Z^* y^* \lambda \qquad (12)$$

The particular form of the function f will be established. Since $-Z^*$ is never greater than C, $f(M) \leq M$. Combining equations (9), (10), (11), and (12) and substituting into equations (7) and (8) one obtains,

$$y = y(-c) \cosh [M - f(M)] / \cosh [Z y^* \lambda + f(M)] \qquad (13)$$

when $dT/dZ \geq 0$

$$y = y(c) \cosh [M + f(M)] / \cosh [Z y^* \lambda + f(M)] \qquad (14)$$

when $dT/dZ \leq 0$

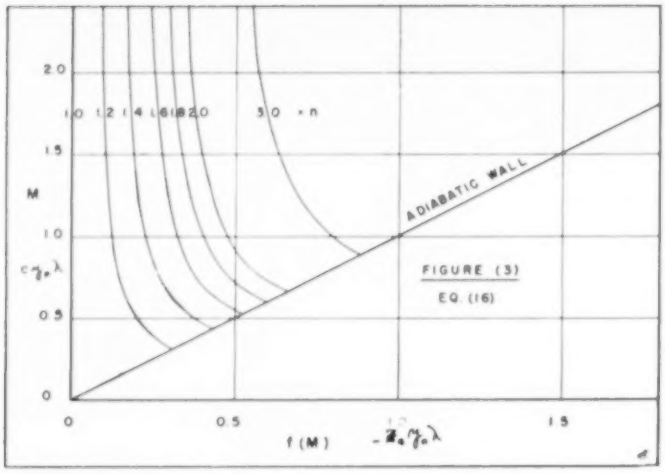
$$n = \cosh [M + f(M)] / \cosh [M - f(M)] \qquad (15)$$

From equation (15) it can be shown that

$$f(M) = \tanh^{-1} [(n - 1) \coth (M) / (n + 1)] \qquad (16)$$

which defines the function f in equation (12). Eq. (16) is plotted in figure 3 for several values of n.

Thus far it has been shown that the temperature distribution across the liquid stream can be represented by equations (13) and (14). If one knows the values for λ, y^*, n, and M the temperature at any position can be readily computed. One needs to know the values of A, S, K, μ' $T(-c)$, $T(c)$, and C to carry out this computation. The



difficulty is that usually the velocity of the boundary is known and not the shearing stress. Therefore, before proceeding with the numerical computations the equations for the velocity distribution will be established.

VELOCITY DISTRIBUTION:

Since $\mu = \mu' e^{-AT} = \mu' y^{-2}$ equation (2) can be written,

$$d u / d Z = S y^2 / \mu' \qquad (17)$$

When the expressions (13) and (14) are substituted into equation (17) and integrated for the limits **O** to **u** and **-C** to **Z** when **d T / d Z** $\geq$ **O** and for u^* to u and Z^* to **Z** when **d T / d Z** $\leq$ **O**, the results are,

$$u = \frac{S \cosh^2 [M - f(M)] \tanh [Z y^* \lambda + f(M)]}{\mu(-c) y^* \lambda} \qquad (18)$$

for d T / d Z $\geq$ O, and

$$u = u^* + \frac{S \cosh^2 [M + f(M)] \tanh [Z y^* \lambda + f(M)]}{\mu(c) y^* \lambda} \qquad (19)$$

for d T / d Z $\leq$ O

Substituting the limiting condition (3) into eq. (18) one obtains an expression for u^*.

$$u^* = \frac{S \cosh^2 [M - f(M)] \tanh [M + f(M)]}{\mu(-c) y^* \lambda} \qquad (20)$$

Combining equations (19) and (20) one obtains,

$$U = \frac{S}{y^* \lambda} \left[\frac{\cosh^2 [M + f(M)]}{\mu_c} + \frac{\cosh^2 [M - f(M)]}{\mu_{-c}} \right] \tanh [Z y^* \lambda + f(M)] \qquad (21)$$

for d T / d Z $\leq$ O

Noting that $\mu(-c) = \mu(c) / n^2$ and from eq. (15) $n^2 = \cosh^2 [M + f(M)] / \cosh^2 [M - f(M)]$,

when the boundary conditions (4) are substituted into equation (21) one obtains,

$$u(c) = \frac{2 S \cosh^2 [M + f(M)] \tanh [M + f(M)]}{\mu(c) y^* \lambda} \qquad (22)$$

Substituting the value for y^* given in eq. (9), the final expression for the velocity of the upper plate is obtained.

$$U_c = \left[\frac{8 J K}{A \mu_c} \right]^{\frac{1}{2}} \sinh \left[C y^* \lambda + \tanh^{-1} \left(\frac{n-1}{n+1} \coth C y^* \lambda \right) \right] \qquad (23)$$

It is interesting to note that when $Z^* = -C$, the case of no heat flow into the lower boundary, that the value of f **(M)** is **M** and the last term of equation (23) reduces to sinh **(2M)**. The value of **n** is then,

$$n = \cosh [2 C y^* \lambda] \qquad (24)$$

When $Z^* = O$, that is when $T(-c) = T(c)$, the value of f **(M)** is zero and **n** = 1. The last term of equation (23) reduces to the sinh **(M)**.

The value of $C y^* \lambda$ is completely defined by equation (23) when one is given values for **A, K,** $\mu(c)$, $T(-c)$, $T(c)$, and $u(c)$. The solution of equation (23) is readily obtained and is plotted in figure 2 for several values of n. The velocity group, ψ, is plotted as abscissa and the shear stress group, **M**, is plotted as ordinate.

$$\psi = [A \mu(c) U^2(c) / 8 J K]^{\frac{1}{2}} = \sinh [M + f(M)] \qquad (25)$$

COMPUTATION OF SPECIFIC PARAMETERS:

The maximum temperature, T^*, is obtained by combining equations (23) and (14) when $y = y^*$,

$$T^* = T_c + \frac{1}{A} \log_e \left[1 + \frac{\mu_c U_c^2}{8 J K} \right] \qquad (26)$$

This equation states that the maximum temperature rise, $(T^* - T(c))$ is completely determined by the material properties A, $\mu(c)$, and **K**, and the velocity of the upper plate. This temperature rise is independent of the plate separation. The maximum temperature increases with increasing velocity, decreasing **A**, decreasing thermal conductivity, and is greater for more viscous materials. When the temperature of the lower boundary is increased the value of T^* does not change until thermal energy is caused to flow into the material thru the boundary. The position of the maximum temperature moves towards **Z** = **-C** as $T(-c)$ is increased but does not rise until after $Z^* = -C$ and only then if the boundary is at a higher temperature. For the system under discussion in this paper $T^* \geq T(-c)$.

To compute the temperature and velocity distributions the following procedure is suggested:

(1) The quantity $Cy^*\lambda$ is calculated from eq. (25) or figure (2).

(2) The parameter $Z^* y^* \lambda$ is obtained from eq. (12) and (16) or figure (3).

(3) The parameter y^* is obtained from eq. (9) or from eq. (26) and the definition, $y^* = e^{AT^*/2}$.

(4) The parameter λ is computed by combining (1), (2), (3) above

$$\lambda = C y^* \lambda / C y^* = M / C y^*$$

(5) The parameter Z^* is obtained from (2), (3), (4) above.

$$Z^* = Z^* y^* \lambda / y^* \lambda = - C f(M) / M$$

(6) The velocity distribution is obtained from eq. (19) and (21).

(7) The temperature distribution is computed from eq. (13) and (14).

(8) The shearing stress can be computed from λ

$$S = [2 J K \mu' \lambda^2 / A]^{\frac{1}{2}}$$

If equations (25) and (9) are combined one obtains an interesting relationship between shearing stress and velocity described by equation (27) and plotted in figure 4.

$$\left[\frac{A C^2 S^2}{2JK U_c}\right]^{\frac{1}{2}} = \frac{\sinh^{-1}\left[\frac{A U_c U_c^2}{8JK}\right]}{\left[1 + \left(\frac{A U_c U_c^2}{8JK}\right)\right]^{\frac{1}{2}}}$$

For a given liquid and plate separation the shearing stress, S, increases to a maximum at $\psi = 1.5$ and then decreases.

NUMERICAL EXAMPLES:

Weltmann and Kuhns (9) computed the temperature distribution across the gap of a rotational viscometer for two materials, an Oronite polybutene oil and a silicone fluid. To compare their results with values calculated by using the equations developed in this paper one can compute the value of T* and Z* or if a more extensive comparison is desired the complete distributions can be compared.

For the Silicone fluid No. 5101C the data are:

K = 3.71×10^{-4} Cal. / sec. Cm. °C

$\mu(c)$ = 605 poise

T(c) = 30.0°, T(-c) = 37.65°C

A = 0.018° C $^{-1}$ obtained from figure 4 ref. (9)

u (c) = 47.1 cm. /sec.

C = 0.1 Cm.

J = 4.18×10^7 ergs / cal.

$$T^* = 30.0 + 1/0.018 \ln\left[1 + \frac{0.018 \times 605 \times 47.17^2}{8 \times 4.18 \times 10^7 \times 3.71 \times 10^{-4}}\right]$$

T* = 39.9°C and from Fig. I ref. (9) T* = 42.5°C.

$\psi = [0.018 \times 47.1^2 \times 605 / 8 \times 4.18 \times 10^7 \times 3.71 \times 10^{-4}]^{\frac{1}{2}} = 0.442$

n = e exp. [A (T (-c) — T (c)) / 2] = 1.071

M = 0.32

f (M) = .11

$y^* \lambda$ = M / C = 3.2

Z* = — f (M) / $y^* \lambda$ = -0.034 Cm.

The value of Z* estimated from figure 1 ref. (9) is -0.03 cm. which is a satisfactory check.

For the Oronite oil No. 32 the data are:

K = 3.06×10^{-4} cal /sec. cm. °C

$\mu(c)$ = 675 poise

T(c) = 30.0 °C, T(-c) = 37.65°C

A = 0.08 °C $^{-1}$ (obtained from fig. 4 ref. (9)

u (c) = 47.1 cm. /sec.

C = 0.1 Cm.

J = 4.18×10^7 ergs / cal.

$$T^* = 30.0 + 1/0.08 \ln\left[\frac{1 \times 0.08 \times 675 \times 47.1^2}{8 \times 4.18 \times 10^7 \times 3.06 \times 10^{-4}}\right]$$

T* = 39.7°C and from figure 2 ref. (9) T* = 41°C.

ψ = 1.081

n = 1.36

M = 0.67

f (M) = 0.27

$y^* \lambda$ = 6.7

Z* = —.04 Cm. and from fig. 2 ref (9) Z* = —.04 cm.

Merrill (4) measured the temperature rise for 10% Buna N in aniline being sheared in a rotation viscometer. He concluded that the temperature variation obtained was due to variations in the cooling fluid temperature. One can compute the temperature rise for his system as was done in the preceding examples. The data are:

K (aniline) = 0.039 in. # Force / sec. in. °C

$\mu(c)$ = 9.3 poise

T(c) = 21°C

A = (aniline) = 0.025°C^{-1} (estimated)

u(c) = 40.1 in / sec.

C = 0.003 in.

J = 1 (if poise are converted to # force sec. / in.^{2})

$$T^* - T(c) = \frac{1}{0.025} \ln\left[1 + \frac{0.025 \times 9.3 \times 1.453 \times 10^{-1} \times 40.1^2}{8 \times 1 \times 0.039}\right]$$

T* — T(c) = 0.7°C which is less than the observed variation and confirms Merrill's conclusions.

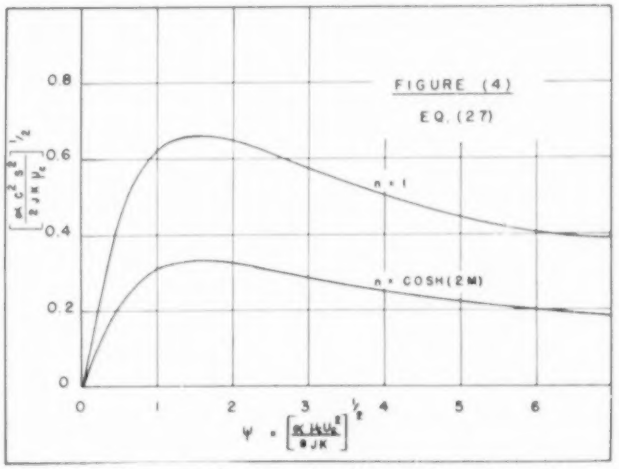
DISCUSSION:

If equations (13) and (14) are solved for the temperature, T, as a function of position, Z, and substitution is made into equation (1), an expression for the viscosity distribution across the liquid stream is obtained. The resulting equation is not convenient for numerical computation and as an alternative it is suggested that one compute the temperature distribution and then obtain the viscosity distribution by direct substitution for each position using eq. (1). The average temperature and average viscosity can be obtained from the plots of temperature and viscosity versus position. $\bar{T}$ does not necessarily correspond to $\bar{\mu}$ and one should compute A and μ' from point values of values of μ and T.

$$\bar{T} = \frac{\sum_c^c T_c \Delta Z}{2C} \quad (28) \qquad \bar{\mu} = \frac{\sum_c^c \mu_c \Delta Z}{2C} \quad (29)$$

An equation for the effective viscosity, which would be measured by a rotational viscometer operating under these conditions, is given in reference (9).

The rational design of plastic processing equipment such as the screw extruder has been limited greatly because of inadequate knowledge concerning the thermal and viscous properties of the materials being processed. Most attempts to correlate viscosity data at conditions



(Alpha in the equations in the figure above is written as A in the text.)

simulating those encountered in practical processes have failed to separate the temperature dependence from other sources of variation. This has restricted the range over which one could scale up the process and hope to obtain predictable results.

The manner in which operating conditions such as peripheral speed and boundary temperatures influence the temperature distribution can be estimated for limiting cases using the methods outlined in this paper. Meaningful viscosity data can be obtained using conventional rotation viscometers, and not restricting their range for isothermal operation. Instead one should measure the boundary temperatures and report a viscosity corrected for temperature increases.

The validity of the equations should be checked by means of experiment although it is quite likely that the computed values will be more accurate than those which one can measure. If the system is designed to have the bob boundary adiabatic, that is no heat flow into or out from the bob-plastic interface, then it should be possible to measure the bob surface temperature for comparison with the computed value.

ACKNOWLEDGMENT

The help and advice of Dr. F. D. Stockton who greatly aided in the successful completion of this work is acknowledged with pleasure. The author wishes to thank the Monsanto Chemical Company for the opportunity to pursue the problem and for permission to present this paper.

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The Analysis of Dynamic Tests of Visco-Elastic Materials*

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In this paper methods of analysis of dynamic tests of visco-elastic materials are examined. It is shown that it is important to develop the analysis on the basis of a general stress-strain relation, since the particular form of the relation for the material under test is not known in advance, and any arbitrary assumption about it may lead to contradictions. Such a general method of analysis is discussed for a simple longitudinal stress test, and for the vibrating reed test. References to the published literature indicate that such contradictions appear in currently accepted analyses, and their influence is detailed.

In seeking a visco-elastic model of springs and dashpots to represent the behaviour of the material, the method presented separates the analysis of the test results from the determination of the appropriate model.

INTRODUCTION

A common method of measuring the stress-strain characteristics of a visco-elastic material is to measure the motion of a dynamic system in which a specimen of the material is strained. Since the stress-strain characteristics are not known in advance, the dynamics of the system must be analyzed for a general visco-elastic material and the particular law governing the material under test must be deduced from the behaviour of the response. This approach has not always been adopted in the literature on the dynamic testing of polymers, and it has lead to errors in methods of interpreting test results. It is the purpose of this paper to clarify this question by discussing a convenient representation of the dynamic response of a system containing a general visco-elastic element, and to detail errors in the analysis of the vibrating reed test which have arisen in the literature because the theory was based on a special visco-elastic model.

We are concerned with simple configurations in which the stress and strain can each be represented by a single component, denoted by σ and ε respectively. These could be shear or normal components. Methods of testing are usually devised to utilize such simple forms of the stress and strain tensors. Linear behaviour is considered, so that a general visco-elastic relation takes the form

$$P(\sigma) = Q(\varepsilon) \qquad (1)$$

where **P** and **Q** are polynomials in the operator $\mathbf{D} = d/dt$ [1]*. For the well-known simple models, for example the Maxwell and Voigt materials, these polynomials are of zero or first powers in **D**, and the powers increase for more complicated materials. The dynamical analysis of a testing method must be carried out for general **P** and **Q**, and the results represented in such a way that expressions for **P** and **Q** can be deduced which will reproduce the measured behaviour. A particularly convenient arrangement which has been commonly used is to employ forced

*This work was sponsored under a Department of Defense Contract NOrd 11496, with the Bureau of Ordnance, Navy Department.

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*Numbers in square brackets refer to the bibliography at the end of the report.

steady oscillatory loading of angular frequency ω so that the stress and strain can be represented by the real parts of

$$\bar{\sigma}\, e \exp. i\,\omega\, t, \quad \bar{\varepsilon}\, e \exp. i\,\omega\, t \qquad (2)$$

where $\bar{\sigma}$ and $\bar{\varepsilon}$ are complex magnitudes which therefore contain both amplitudes and initial phase angles. If the stress is of this form it follows from (1) that the strain is also, for in this case $\mathbf{D} = i\,\omega$, and (1) becomes an algebraic equation between $\bar{\sigma}$ and $\bar{\varepsilon}$. This leads to the usual notation for complex modulus:

$$\bar{\sigma} / \bar{\varepsilon} = \bar{\sigma} / \bar{\varepsilon} = E = E_1(\omega) + i E_2(\omega) = Q(i\omega) / P(i\omega) \qquad (3)$$

and complex compliance:

$$\bar{\varepsilon} / \bar{\sigma} = \bar{\varepsilon} / \bar{\sigma} = J = J_1(\omega) - i J_2(\omega) = P(i\omega) / Q(i\omega) \qquad (4)$$

This is not the only modulus which one could use, but it forms a convenient variable with which to measure the visco-elastic characteristics of a material. For a material obeying a relation of the type (1) it is known that analytically a knowledge of $E_1(\omega)$ or $E_2(\omega)$ in general prescribes the material properties completely (see for example the equivalent electrical problems discussed in [2], chap. XIV). For the analysis of experimental results which comprise values of E_1 and E_2 within the limits of experimental error over a finite frequency range, methods will be described elsewhere [9] for using both functions to determine a simple representation of the type (1) which reproduces the modulus variation approximately.

A more general complex modulus could be associated with response at complex frequency, for example:

$$\sigma = \bar{\sigma}\, e \exp(-\gamma + i\,\omega)t\,, \quad \varepsilon = \bar{\varepsilon}\, e \exp(-\gamma + i\,\omega)t \qquad (5)$$

and (1) would determine a complex modulus $Q(-\gamma + i\,\omega) / P(-\gamma + i\,\omega)$. Such a modulus would be more difficult to analyse than that corresponding to steady oscillation, since it is in general a function of two independent parameters. Such a modulus is obtained from measurements of the free oscillations of a system containing a visco-elastic element. Changes in the system to produce oscillation at varying complex frequency will in general modify both the real and complex components of the frequency which will introduce difficulties in interpreting the results. Steady state forced oscillation which provides response over a range of values of real frequency comprises a simpler representation of dynamic response, and this paper is mainly concerned with this type of test.

One of the commonest such methods of testing is to utilize a vibrating reed as shown schematically in Fig. 1. A reed of the material to be tested is clamped at one end to prevent rotation, the other end being free. The clamped end is given a steady oscillatory motion of amplitude δ_0 and frequency ω. The dynamic visco-elastic properties of the material are deduced from measurements of the oscillation of the free end over a range of frequency. Linearity can be checked by varying the amplitude δ_0; for a linear material δ will vary proportionally.

As illustrated in the next section of this paper for a simpler problem, the test results can be most easily analysed if the components of the displacement of **B** in phase and 90° out of phase with the steady oscillation of **A** are both measured, or equivalently, if the amplitude and phase of the oscillation of **A** are measured. In practice for the vibrating reed this proves to be a difficult task, and it is common to measure the amplitude of δ only. Thus for each frequency less information is obtained, but by taking measurements at resonance, for which the frequency is varied until maximum response is obtained, complete information about the complex modulus can be obtained. The resulting analysis is relatively simple if E_2, the imaginary part of the complex modulus is less than half the real part E_1. In order to obtain a series of resonance measurements over a range of frequency, the system must be modified, for example by shortening the reed, to produce a range of resonance frequencies. It is in the analysis of such a series of test results that care must be taken not to assume a visco-elastic model in order to interpret the results. Much of the difficulty would disappear if phase differences could also be measured, and this is achieved in some dynamic systems for measuring the physical properties of high polymers (see for example the Fitzgerald Apparatus [3]).

In the next section these questions are illustrated for a simple system of a rigid mass suspended from a rod of the material to be tested. The dimensions and frequencies used are considered to be such that the mass of the rod can be neglected so that simple uniform tensile or compressive stress occurs in the rod.

THE ANALYSIS OF A SIMPLE SYSTEM

The system to be considered is shown in Fig. 2. The support at the upper end permits vertical motion u_0 without rotation. The mass **M** at the lower end moves vertically with displacement u. If **l** is the length of the test specimen, and **A** its cross-sectional area, the longitudinal strain is $(u - u_0)/l$, and the equation of motion of the mass is:

$$M D^2 u = -A\sigma = -A (Q / P) [u - u_0) / l] \qquad (6)$$

in which the stress-strain relation takes the form (1), and formal division by the operator **P** has been carried out for convenience of expression.

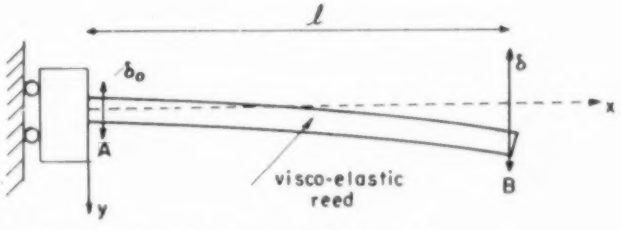

FIG. 1. THE VIBRATING REED TEST

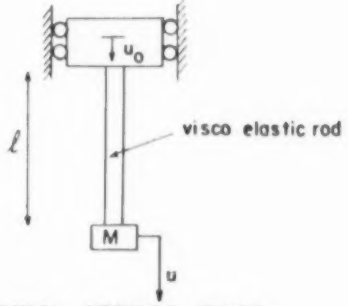

FIG. 2. A LONGITUDINAL STRESS TEST

For the case of free oscillations u_0 is zero, and the resulting strain can take the form

$$\varepsilon = \varepsilon_0 e \exp \gamma t = \varepsilon_0 e \exp (-\lambda + i \omega) t \qquad (7)$$

and substitution in (6) determines:

$$(Q / P) (\gamma) = -(M l / A) \gamma^2 \qquad (8)$$

where for this simple type of motion, the operators **Q (D), P (D)** become the complex functions **Q** (γ), **P** (γ). Thus if a single γ, which contains both the frequency component ω and the decay component $-\lambda$, is measured for a free oscillation, the complex modulus for complex frequency, **(Q / P)** (γ) = **E** (γ), is given directly by (8). However, as mentioned in the previous section, this is a rather complicated concept to utilize. For example if the rod were a Voigt material satisfying the relation

$$\sigma = (E_1 + \nu_1 D) \varepsilon \qquad (9)$$

$$\gamma = [-\nu_1 \pm i \sqrt{(4 E_1 M l / A) - \nu_1^2}] / (2 M l / A) \qquad (10)$$

$$E(s) \cdot \mathcal{L}_t = (E_1 - \frac{\nu_1^2 A}{2 M \ell}) + i \nu_1 \sqrt{\frac{A}{M \ell} \left(E_1 - \frac{\nu_1^2 A}{4 M \ell}\right)} \qquad (11)$$

In the case of forced steady oscillation with frequency ω we have simply

$$E (\omega) = E_1 + i \omega \nu_1$$

The form of (11) indicates that even for a very simple model the utilization of complex frequency, which is implicit in damped free vibration, expresses visco-elastic properties in a very complicated manner, compared with steady forced oscillation. If the damping coefficient is sufficiently small compared with the real part of the frequency, ω, this complexity can be avoided by using an approximate analysis (see [4], p. 701).

For steady forced oscillation $u_0 = \bar{u}_0 e \exp i \omega t$, $u = u e \exp i \omega t$, where $\bar{u}_0$ and $\bar{u}$ are complex quantities. (6) reduces to an expression for the response ratio as a function of the frequency ω

$$\frac{\bar{u}}{\bar{u}_0} = c = \frac{1}{1 - \frac{P(i\omega)}{Q(i\omega)} \frac{M \ell \omega^2}{A}} = \frac{1}{1 - \psi} \qquad (12)$$

where ψ is defined as

$$\psi (\omega) = \psi_1 + i \psi_2 = P (i \omega) M l \omega^2 / Q (i \omega) A \qquad (13)$$

Alternatively we can express the modulus or compliance (Eqn. (3) and (4)) as a function of the response ratio c:

$$E (\omega) = E_1 + i E_2 = M l \omega^2 c / A (c - 1) \qquad (14)$$

$$J (\omega) = J_1 - i J_2 = A (1 - 1 / c) / M l \omega^2 \qquad (15)$$

Thus if the complex response ratio c is measured for a range of forcing frequencies ω, (14) and (15) give the complex modulus and compliance directly.

If only the ratio of the amplitudes | c | can be measured it is not possible to determine the complex modulus directly. However, if a series of measurements of this ratio at resonance is made for a range of resonance frequencies obtained by varying the mass **M**, the variation of complex modulus with frequency can be determined. To illustrate the significance of a resonance measurement, (12) gives the amplitude ratio in the form

$$|c| = \frac{1}{|1 - \psi|} = \frac{1}{\sqrt{(1 - \psi_1)^2 + \psi_2^2}} \qquad (16)$$

Thus we can plot the amplitude ratio on the ψ plane, and as shown in Fig. 3 the contour lines are concentric circles with center (1, 0). As the radius decreases the amplitude ratio increases, and it approaches infinity as the center is approached for this point corresponds to resonance of a purely elastic system. The expression for the response in terms of the function ψ is not dependent on the particular forms of **P** and **Q** corresponding to a given material. The response relations (12) and (16) can therefore be used to analyse test results before the operators **P** and **Q** have been determined.

A resonance test consists of varying the frequency around that which produces maximum response, and in view of (16) this occurs when ψ is closest to unity. This varying frequency is associated with fixed values of **M**, **l** and **A**, so that in view of (13) response values will be measured on a trajectory in the ψ plane, such as ABC in Fig. 3. Since P / Q = 1 / E, E_2 positive demands that ψ_2 be negative. The trajectory ABC will be given by (13) with variable ω, and will thus depend on the form of the operators **P** and **Q**. For different resonance frequencies obtained by changing **M**, the corresponding trajectory will change according to an expansion by a constant factor about the origin. This will of course bring a different range of ω, and correspondingly of **P** (i ω), **Q** (i ω) into the region $\psi \propto 1$, which influences the measurement at resonance.

For the trajectory ABC, the maximum response occurs at **B** where the curve is tangent to a contour. In the case $E_2 << E_1$, so that $\psi_2 << \psi_1$, we can use measured values of resonance response, combined with the condition for the maximum at resonance, to deduce E_1 (ω) and E_2 (ω). For with $\psi_2 << \psi_1$, the trajectory ABC will be inclined at a small angle to the ψ_1 axis in view of the definition (13). The point of tangency with the contours, **B**, therefore lies close to the vertical diameter $\psi_1 = 1$. Writing

$$\psi = 1 - \varepsilon - i b \qquad (17)$$

in the region of resonance this requirement demands $1 >> b >> | \varepsilon |$. In terms of ε and **b** the amplitude ratio of response to forcing displacement is given by:

$$| c |^2 = 1 / (\varepsilon^2 + b^2) \qquad (18)$$

which follows directly from (16) and (17).

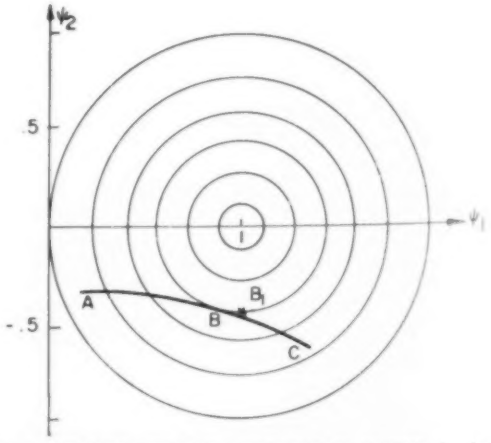

FIG. 3. CONTOURS OF THE RESPONSE RATIO $|u/u_0|$

The measured value of $|c_j|$ at the resonance frequency ω_j thus provides a first approximation for ψ_1 given by $\varepsilon = 0$, $b = 1 / |c_j|$ corresponding to the point B in Fig. 3. That is:

$$\psi_{j1} = 1 - (i / |c_j|) \tag{19}$$

(15) determines the corresponding values of the compliance to be:

$$J_1(\omega_j) = i J_2^1(\omega_j) = (A / M l \omega_j^2)[1 - (i / |c_j|)] \tag{20}$$

Such a first approximation can be obtained for each value of ω_j for resonance with different values of the mass M. These results provide a first approximation to $J_1(\omega)$, $J_2(\omega)$ from which an approximation to the trajectory ABC for each resonance point can be obtained, leading to a more accurate value for the compliance at the resonance point **B**.

Resonance occurs when

$$d|c| / d\omega = 0 \tag{21}$$

for constant **M**, **A** and **l**. Using (18) this reduces to

$$\varepsilon \, d\varepsilon / d\omega + b \, db / d\omega = 0 \tag{22}$$

and the derivatives can be expressed in terms of the derivatives of the compliance using equations (12, 13, 14, 15 and 17)

$$\varepsilon = 1 - J_1 (M l \omega^2 / A) \quad ; \quad b = J_2 (M l \omega^2 / A) \tag{23}$$

$$d\varepsilon / d\omega = -(M l / A)(\omega^2 \, dJ_1 / d\omega + 2\omega J_2); \quad db / d\omega = (M l / A)(\omega^2 \, dJ_2 / d\omega + 2\omega J_2) \tag{24}$$

Using the values of J^1 and $dJ^1 / d\omega$ given by the first approximation, in eqs. (24), (22) and (18) together with the measured value of $|c|$ a second approximation for ε and **b** can be determined and so of $J_1(\omega_j)$ and $J_2(\omega_j)$. The resulting changes in **J** will usually be sufficiently small for this approximation to correspond to the correct resonance point **B** in Fig. 3. If not the iteration can be repeated. Thus the real and imaginary part of the compliance can be determined from measurements of the amplitude ratio at resonance only, without a measure of the phase difference. The gain in experimental simplicity is, however, balanced against considerably greater analytical complexity. In practice, quite apart from the considerations discussed here, resonance methods are convenient because the response reaches its maximum amplitude and is therefore amenable to more accurate measurement.

THE VIBRATING REED TEST

The interpretation of the vibrating reed test closely parallels the analysis given in the previous section for a simple dynamic longitudinal stress test. Since the dimensions of the cross-section of the reed are usually small compared with its length, a development of the usual elastic beam vibration theory can be used with Young's modulus **E** replaced by the operator **Q / P**. The equation for lateral displacement y in terms of the mass per unit length m, and the second moment of the section I, becomes (see for example [5]

$$(Q / P) I \partial^4 y / \partial x^4 + m \partial^2 y / \partial t^2 = 0 \tag{25}$$

The use of the operator **Q / P** in (25) is formal, and to obtain a partial differential equation, we must multiply by **P** giving

$$I Q \partial^4 y / \partial x^4 + m P \partial^2 y / \partial t^2 = 0 \tag{26}$$

The order of this equation depends on the operators P and Q. It can be developed directly from the equation of motion and the assumption that plane sections of the reed remain plane, just as the elastic case is developed.

In the vibrating reed test we consider steady forced oscillation with frequency ω. Thus the displacement **y** is of the form:

$$y = \bar{y} \, e \exp i \omega t \tag{27}$$

so that the operator $D = \partial / \partial t$ can be replaced by multiplication by $i\omega$. (25) and (26) become

$$d^4 \bar{y} / dx^4 - [m \omega^2 P(i\omega) - I Q(i\omega)] \bar{y} = 0 \tag{28}$$

This is similar to the equation for forced oscillation of an elastic rod, with the difference that the coefficient of $\bar{y}$ is in this case complex, and dependent on the complex modulus $E(\omega) = Q(i\omega) / P(i\omega)$. (28) can be solved directly to satisfy the boundary conditions in the simplest case of an unloaded reed:

$$x = 0 \;, \quad \bar{y} = \delta_0 \;, \quad \partial \bar{y} / \partial x = 0 \tag{29}$$

$$x = l \;, \quad \partial^2 \bar{y} / \partial x^2 = \partial^3 \bar{y} / \partial x^3 = 0$$

Solution of the differential equation determines the motion of the free end to be given by

$$y(t) = \bar{y}(l) e^{i\omega t} = \delta_0 e^{i\omega t} \frac{\cosh \Phi + \cos \Phi}{1 + \cosh \Phi \cos \Phi} e^{i\omega t} \tag{30}$$

where Φ is defined as:

$$\Phi = \left[\frac{m\omega^2 P(i\omega)}{I Q(i\omega)}\right]^{\frac{1}{4}} l = \Phi_1 + i\Phi_2 \tag{31}$$

δ / δ_0, the amplitude ratio between the free and clamped ends of the reed shown in Fig. 1, is a complex number indicating that the motions of the two ends of the reed are in general not in phase. The modulus $|\delta / \delta_0|$ only is normally measured in a vibrating reed test.

The situation represented by (30) and (31) for the vibrating reed parallels closely (12) and (13) of the last section. The response amplitude ratio for steady forced oscillation is given by (30), and this relation is independent of the material properties expressed by the operators **P** and **Q**. For varying frequency with a given reed, which represents the conditions occurring in a resonance test, (31) gives the trajectory in the Φ plane which determines the variation of the response. The response function (30) is much more complicated than (12), and contains an infinite series of resonance peaks corresponding to the modes of vibration of an elastic reed. In practice measurements are usually taken of excitation at frequencies in the region of the natural frequency of the fundamental mode of the reed. This provides the lowest resonance frequency. Contours of the modulus of the response ratio in this region are shown in Fig. 4. Resonance for the fundamental mode of the elastic system is represented by the point **D**. **E** represents a saddle point, and beyond it the response ratio begins to rise again to the second resonance peak. Because of the fourth root in (31), points within the angle CAE and on its radii represent all posi-

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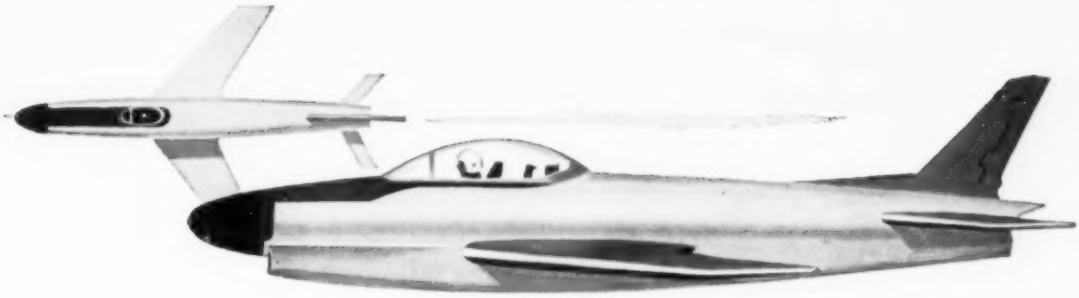

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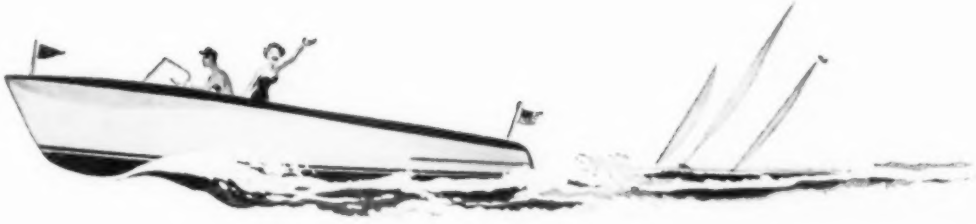

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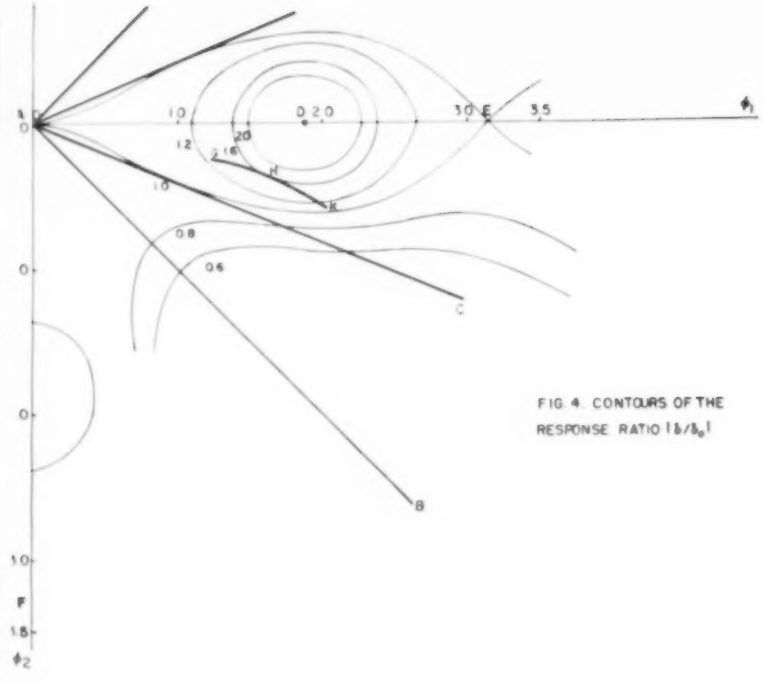

FIG. 4. CONTOURS OF THE RESPONSE RATIO $|\delta/\delta_0|$

tive values of E_1 and E_2 from zero to infinity. Thus only this region is needed for the interpretation of the vibrating reed test. The angle CAF includes negative values of the moduli. The same function is repeated in the other quadrants corresponding to the three other fourth roots of the complex number in (31). For this reason the variation of the amplitude ratio along AF is the same as along AE. The contours are symmetrical about AB.

A vibrating reed resonance test can be analyzed on the basis of (30) and (31) just as the simpler longitudinal test was treated in the previous section. The analysis is considerably more involved, particularly for a loaded reed, and will be given elsewhere [10]. As the frequency is varied around the resonance frequency, a trajectory is traversed in the ϕ plane, such as GHK in Fig. 4. Maximum response occurs at H where the trajectory is tangent to a contour line. To obtain the real and imaginary part of the modulus from amplitude measurements only, a series of resonance measurements must be made, for example by varying the length l of the reed. This varies the fundamental frequency, and determines a series of different trajectories in the region of the resonance peak **D**. The method of analysis mentioned above determines these resonance points H, and the corresponding complex modulus or compliance at each resonance frequency.

In the analysis of the vibrating reed test it has been common (see for example [6] and [7]) to use the basic partial differential equation for lateral vibration of a reed of Voigt material:

$$u\,\partial^2 y / \partial^2 t + E_1 I\,\partial^4 y / \partial x^4 + \eta I\,\partial^5 y / \partial x^4 \partial t = 0 \qquad (32)$$

in place of the more general partial differential equation (26). To make this restriction at the beginning strictly speaking prescribes the type of variation of complex modulus with frequency that can be treated, but this difficulty is avoided by permitting E_1 and η in (32) to vary with frequency. This of course is a meaningless modification of (32) as a partial differential equation, and only has significance when consideration is restricted to steady sinusoidal solutions with respect to time. This difficulty can be overcome as in the present analysis by using (26) with **P** and **Q** unspecified. While the above can be considered as purely a formal criticism, we shall see below that it has lead to errors in the analysis of resonance tests.

Nolle's [8] approach, which represents the reed by an equivalent single degree of freedom electric circuit, suffers from the equivalent restriction as detailed below. However, in analysing the resonance test, Nolle points out this short-coming (p. 773 of [8]) but accepts it in order to simplify the analysis. From a practical standpoint the error involved is not significant if E_2 is small compared with E_1. Hillier [6] also limits himself to an approximate analysis. In contrast, Horio and Onogi [7] claim an exact analysis to clear up the difficulties mentioned by Nolle. However, the limitations of their analysis do not permit the higher order terms to be determined correctly.

Horio and Onogi's analysis is based on expansion of the expression for the modulus of the amplitude ratio (which would be obtained directly from eqn. (30) above) for $E_2 << E_1$. ϕ, defined in (31), is written as a + i b, and resonance is defined by the condition

$$(d / d a)\,(\delta^2 / \delta_0^2) = 0 \qquad (33)$$

In Fig. 4 this is equivalent to prescribing the trajectory GHK to be parallel to the ϕ_1 axis, which effectively prescribes the form of **P** and **Q** for the frequency variation in the resonance test. A resonance analysis based on this premise can only give the first approximation similar to that discussed in the previous section (this corresponds to the point B_1 in Fig. 3). For a more thorough analysis, the influence of the particular forms of **P** and **Q** corresponding to the material under test must be included, as in the analysis detailed in the previous section. The forms of **P** and **Q** are to be determined from the test results, so that an iteration procedure of the type used is necessary to obtain their behaviour and include its effect in the analysis.

We thus see that the main difficulty in interpreting a resonance test is that the conditions under varying frequency correspond to a certain trajectory such as GHK in Fig. 4. If an initial assumption is made such as that by Horio and Onogi that GHK is horizontal, in general the deduced modulus variation will not be consistent with this assumption. Nolle's first order analysis which is based on an equivalent electric circuit with fixed components in effect assumes that E_1 and E_2 / ω are constant. For a particular resonance measurement, this will prescribe a certain trajectory in the ϕ plane, but other than by coincidence this will not agree with that based on the deduced variation of E_1 and E_2 with frequency. Strict application of the vibration equation (32) also prescribes this trajectory in advance. For an accurate analysis, freedom must be retained initially, and the trajectory determined in conformity with the deduced material properties.

In the above discussion we were concerned with deduction from the measured amplitude response ratio at resonance. If instead the width of the resonance peak is measured for which the amplitude is $1 / \sqrt{2}$ of the maximum, the need to analyze the results by considering the correct trajectory will be more important, since measurements over an extended length of the trajectory are involved. A trial and error method would be needed in conjunction with a contour diagram such as Fig. 4.

DISCUSSION

We have seen above that it is possible to analyze a dynamic test of a visco-elastic material on the basis of the general stress-strain relation (1), and to interpret the results in the form of the determination of $\mathbf{Q}(i\omega) / \mathbf{P}(i\omega) = \mathbf{E}(\omega)$ over the range of ω tested. This function represents the mechanical properties of the material in this frequency range. The problem of determining a corresponding visco-elastic model composed of dash-pots and springs then consists of obtaining a model which reproduces $\mathbf{E}(\omega) = \mathbf{E}_1(\omega) + i\,\mathbf{E}_2(\omega)$ for the modulus as a function of frequency. In the analysis of the test itself, the existence or form of the model does not appear. As detailed above, it is important that the interpretation of test results should be carried out consistently with this concept in mind, in order to avoid placing unwarranted constraints on the analysis.

The visco-elastic relation (1) has been used in this paper in place of the more general definition of a hereditary material with a continuous, rather than discrete, spectrum of relaxation times. The analysis in fact includes the more general concept since $\mathbf{Q}(i\omega) / \mathbf{P}(i\omega)$ is sought without restriction as to its form. As deduced from (1) it must be a rational function, but is not so restricted in the general hereditary case. In the practical analysis of experimental results this distinction does not arise since approximation within experimental error is all that can be sought. If behaviour over a limited frequency range only is required, as for example in the analysis of the motion of a system within a limited period of time, comparatively simple forms for $\mathbf{P}$ and $\mathbf{Q}$ will provide a satisfactory representation of the material. A correspondingly simple visco-elastic model can be utilized. Studies of this type have been made on the project of which this work is a part, and methods of deducing such simple approximations will be described elsewhere [9].

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Viscous Heat Effects In Extrusion of Molten Plastics

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When a fluid is forced through a capillary tube at very high shear stresses, heat is produced because of the internal friction of the fluid. Brinkman** has developed a method for solving the differential equations of motion and energy balance to describe the heat effects for newtonian flow in capillaries of circular cross-section. In this paper it is shown how Brinkman's method can be extended to describe the heat effects for the flow of non-newtonian fluids which obey a power-law relation between the coefficient of viscosity and the shear stress. The temperature profiles are calculated for one particular power law corresponding to the flow of a general purpose polyethylene melt. Calculations are made for two cases: (1) the capillary walls are maintained at the temperature of the feed, and (2) the capillary walls are thermally insulated. It is shown how the temperature profiles change as the fluid flows down the tube, and the average and maximum temperatures are calculated. The effect of the viscous heat production on the rheometer flow curves is then calculated approximately. The theory predicts satisfactorily the value of the shear stress for which the viscous heat effects begin to be important and also gives a flow curve the shape of which is in good agreement with the experimental flow curve.

National Science Foundation Grant G-356

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INTRODUCTION

When molten plastics are extruded through fine capillaries at high shear stresses, a temperature distribution can result from the heat produced by viscous dissipation. It is of interest to study the form of the resulting temperature profiles and the order of magnitude of the temperature effects, in order better to interpret the flow curves of plastics melts and also to know whether or not the temperature effects are sufficiently large to produce changes in the molecular or bulk properties of the polymers.

Molten plastics are non-newtonian fluids, and heretofore only rough calculations have been made for the heat effects in non-newtonian flow through capillaries of cylindrical cross-section.[1] The corresponding flow problem for newtonian flow has, however, been the subject of several investigations. Hersey[2] was the first to suggest that deviations from Poiseuille's law at high shear stresses could be interpreted in terms of viscous heat effects, and he gave simple explanations of the effect for both the iso-

thermal wall and the insulated wall problems. Subsequently Hersey and Zimmer[3] considered the problem from dimensional arguments. A very extensive investigation was carried out by Philippoff[4], who unfortunately based his treatment on an incorrect differential equation. Philippoff's work has been discussed in great detail by Hausenblas[5], who has considered viscous heat effects in flow through slits and tubes. Hausenblas has not, however, given any comparisons between calculated and experimental results, nor has he taken into account the transport of heat by convection. By far the most careful treatment is that of Brinkman[6], who has considered the heat convection contribution for both the isothermal wall and the insulated wall problems.

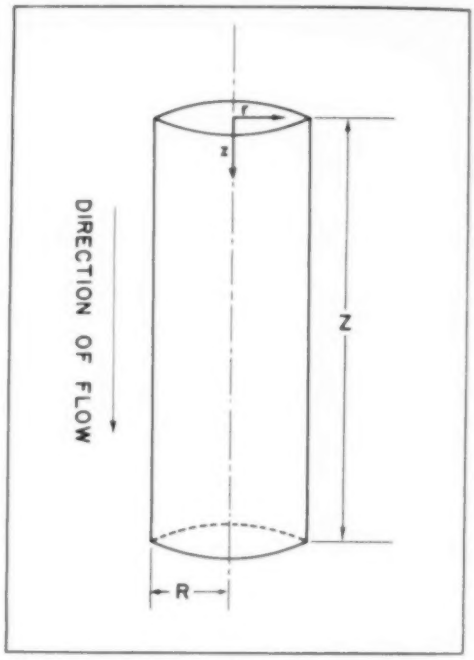
In addition to the above-mentioned treatments of flow through capillaries, there have been several other analytical solutions for flow with viscous heat effects in other simple geometries. Problems on flow in concentric cylinder viscometers have been studied by Weltmann and Kuhns[7] and summarized by Blok[8]. Flow near a rotating plate has been examined by Millsaps and Pohlhausen[9].

It is the purpose of this paper to show how Brinkman's work on flow through capillaries may be extended to describe heat effects for some types of non-newtonian flow, and to calculate the temperature profiles for one particular type of molten plastic. The effect of this temperature distribution on the flow curves for the molten plastic are considered briefly.

THE DIFFERENTIAL EQUATION FOR THE TEMPERATURE DISTRIBUTION IN NON-NEWTONIAN FLOW

The basic equations which describe the flow of a fluid in a non-isothermal system are the differential equations of continuity, motion, and energy balance, which are the statements of the principles of conservation of mass, momentum, and energy, respectively. For a fluid made up of a single chemical component, these equations in their most general form contain quantities known as the "fluxes": the momentum flux (or pressure tensor) and the energy flux. In order to utilize the basic equations, these fluxes have to be expressed in terms of the gradients of the physical properties and the transport coefficients; this is done by means of the thermodynamics of irreversible processes. Inasmuch as the basic flow equations and the expressions for the fluxes have been given recently in two readily available references[10, 11], they are not given again here.

Let us now consider the application of the basic flow equations to the problem of the extrusion of molten plastics through small capillary tubes of circular cross section at such high shear rates that the heat produced by viscous dissipation is of importance. Two problems will be considered: the **isothermal wall** problem, in which the wall of the capillary is maintained at the temperature of the molten stock; and the **insulated wall** problem, in which it is assumed that no heat can pass through the walls of the capillary tube. The geometry of the problem and the coordinates used are given in Figure 1.



1. Schematic illustration of flow in a capillary tube.

For the purpose of the mathematical development the following assumptions are made:

1. The fluid is incompressible
2. The flow is steady (all partial derivatives with respect to time are zero)
3. End effects are neglected, with respect to the development of the velocity profile
4. External forces, such as gravity, are neglected
5. The axial component of the velocity is assumed to be a function of the radial distance **r** alone. The radial and tangential components of the velocity are assumed zero.
6. The coefficient of shear viscosity μ is asumed to be independent of temperature and pressure, but dependent upon shear stress (and hence upon position)
7. The coefficient of thermal conductivity λ is assumed to be independent of pressure and temperature
8. Laminar flow prevails throughout
9. There is no "slip" at the walls
10. Heat transfer in the **z**-direction by conduction is small compared with that by convection
11. The non-newtonian nature of the fluid can be taken into account by using the ordinary newtonian expression for the momentum flux, but with the coefficient of shear viscosity taken to be some assumed function of the local shear stress.

When these assumptions are made, the equations of motion and energy balance reduce to:

$$0 = -\frac{dp}{dz} + \frac{1}{r}\frac{d}{dr}\left(\mu r \frac{dv}{dr}\right) \qquad (1)$$

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$$\frac{\partial T}{\partial z} = \lambda \frac{1}{r}\frac{\partial}{\partial r}\left(r\frac{\partial T}{\partial r}\right) + \mu\left(\frac{\partial v}{\partial r}\right)^2 \qquad (2)$$

in which v is the axial velocity of the fluid, c is the heat capacity per unit volume, and T is taken to be the temperature above the stock temperature at the entrance to the capillary. In (1) the pressure gradient dp/dz may be replaced by the overall pressure drop per unit length $-P/Z$. In (2) the first term is the axial heat transfer by convection, the second term the radial heat transfer by conduction, and the last term is the heat production due to viscous dissipation, [10], [11],[12].

The assumption that the viscosity is temperature independent results in considerable simplification, inasmuch as (1) and (2) are thereby uncoupled, so that the velocity distribution obtained from (1) can be used in (2) to obtain the temperature distribution. Without this assumption not only are the equations coupled, but it also becomes necessary to include both radial and axial components of the velocity in order to satisfy the equation of continuity.

In order to solve (1) for the velocity distribution, one must assume some sort of shear-stress dependence for the coefficient of shear viscosity. Many molten plastics have been shown to obey a "power-law model" over a wide range of shear-stresses. In this discussion we make use of the power law in the following form:

$$\frac{1}{\mu} = \frac{1}{\mu^\circ}\left(\frac{\tau}{\tau^\circ}\right)^\alpha \qquad (3)$$

in which μ° is the vicosity at some "standard" shear stress τ°. The quantity α is a measure of the deviation from newtonian behavior (for newtonian flow $\alpha = 0$ and μ is a constant; for non-newtonian flow $\alpha > 0$ and not necessarily integral). In circular tubes the shear stress varies linearly* with the radial coordinate r. Hence it is more convenient to rewrite (3) thus:

$$\frac{1}{\mu} = \frac{1}{\mu^\circ}\left(\frac{\tau_R}{\tau^\circ}\right)^\alpha\left(\frac{r}{R}\right)^\alpha \qquad (4)$$

in which τ_R is the shear stress at the wall of the capillary where $r = R$. This particular form of the power law is convenient because all of the quantities appearing therein have physically meaningful dimensions, and also because the last two factors give explicitly the change of fluidity with radial distance and the change from experiment to experiment (that is, as the pressuring gradient is altered). The advantages and disadvantages of the power law have been discussed in some detail by Reiner. [13]

The velocity distribution for the power law model is obtained by integration of (1a), in which the expression for μ in (4) is used. This gives:

$$v = \frac{R}{\alpha+2}\left(\frac{\tau^\circ}{\mu^\circ}\right)\left(\frac{\tau_R}{\tau^\circ}\right)^{\alpha+1}\left(1-\left(\frac{r}{R}\right)^{\alpha+2}\right) \qquad (5)$$

This expression simplifies to the usual Poiseuille velocity distribution when $\alpha = 0$. The heat produced by viscous dissipation may then be obtained from (4) and (5):

$$\mu\left(\frac{dv}{dr}\right)^2 = \frac{\tau^{\circ 2}}{\mu^\circ}\left(\frac{\tau_R}{\tau^\circ}\right)^{\alpha+2}\left(\frac{r}{R}\right)^{\alpha+2} \qquad (6)$$

From this it is clear that most of the heat produced by viscous effects is liberated in the vicinity of the wall.

*This is easily seen by integration of (1) with respect to r to give:

$$\mu\,(dv/dr) = -Pr/2Z = -\tau \qquad (1a)$$

in which τ is the local shear stress.

[10] *H. Lamb, Hydrodynamics, Sixth Edition, Dover Publications, New York, (1953)—p. 580*

We now define the following reduced variables, which reduce to those of Brinkman[6] when α is set equal to zero:

$$\rho = \text{reduced radial distance} = \frac{r}{R} \qquad (7)$$

$$\zeta = \text{reduced axial distance} = z \cdot \frac{(\alpha+2)\,\lambda\mu^\circ}{cR^2\,\tau_R\,(\tau_R/\tau^\circ)^\alpha} \qquad (8)$$

$$\theta = \text{reduced temperature rise} = T \cdot \frac{(\alpha+4)^2\,\lambda\mu^\circ}{4R^2\,\tau_R^2\,(\tau_R/\tau^\circ)^\alpha} \qquad (9)$$

In terms of these quantities the substitution of (5) and (6) into (2) gives:

$$(1-\rho^{\alpha+2})\frac{\partial\theta}{\partial\zeta} = \frac{1}{\rho}\frac{\partial}{\partial\rho}\left(\rho\frac{\partial\theta}{\partial\rho}\right) + \frac{(\alpha+4)^2}{4}\rho^{\alpha+2} \qquad (10)$$

This equation is to be solved for the temperature distribution $\theta(\rho,\zeta)$ for the two cases described above for which the boundary conditions are:

ISOTHERMAL WALL
$$\theta = 0 \text{ at } \zeta = 0 \qquad (11)$$
$$\theta = 0 \text{ at } \rho = 1$$
$$\partial\theta/\partial\rho = 0 \text{ at } \rho = 0$$

INSULATED WALL:
$$\theta = 0 \text{ at } \zeta = 0$$
$$\partial\theta/\partial\rho = 0 \text{ at } \rho = 1$$
$$\partial\theta/\partial\rho = 0 \text{ at } \rho = 0$$

For both the isothermal wall and the insulated wall problems, the solution to (10) may be written as the sum of two functions, thus:

$$\theta = \theta_1 + \theta_2 \qquad (13)$$

The first function, θ_1, is the solution to a special simplified case; these solutions are given in the next section. The function θ_2 is the additional function required to give the general solution.

SOLUTIONS TO SIMPLIFIED CASES OF THE DIFFERENTIAL EQUATION FOR THE TEMPERATURE DISTRIBUTION

It is easy to get approximate solutions to the differential equation for both isothermal wall and insulated wall problems, by making additional simplifying assumptions. The results for these special cases are then used to obtain the analytical form for the general solution in the next section.

a. **Isothermal Wall**

If it is assumed that the convection term

$$(1-\rho^{\alpha+2})\,\partial\theta/\partial\zeta$$

in (10) is unimportant, then the temperature distribution depends only on the radial coordinate. The solution of the differential equation consistent with the boundary conditions at $\rho = 0$ and $\rho = 1$ is then:

$$\theta_1 = (1/4)(1-\rho^{\alpha+4}) \qquad (14)$$

For newtonian fluids ($\alpha = 0$) this reduces to the fourth-power temperature distribution obtained earlier by Hersey.[2] Philippoff[4] obtained the same radial dependence, but the factor in front of his expression is incorrect by a factor of 12/16.

Hausenblas[5] has suggested a method for modifying the above result when the temperature dependence of the viscosity coefficient must be taken into account. Brinkman's calculations[6] have shown that when the convective term is taken into account, the temperature profile in newtonian flow deviates considerably from the fourth-power dependence.

b. Insulated Wall

When the conduction term in (10) is omitted and the remaining terms are averaged over the radial coordinate (by multiplying by ρ and integrating from $\rho = 0$ to $\rho = 1$), then the equation may be integrated with respect to the axial coordinate ζ to give.

$$\Theta = \frac{(\alpha+4)^2}{2(\alpha+2)}\,\zeta \qquad \text{or} \qquad T = \frac{P}{c}\,\frac{z}{Z} \tag{15}$$

This result was also obtained by Hersey for newtonian fluids,[2] and states that, to a first approximation, the temperature increases linearly down the tube and depends solely on the pressure drop ρ and the heat capacity (per unit volume) of the fluid, **c**.

Substitution of (15) into (10) and subsequent integration with respect to ρ with the boundary condition that $d\theta/d\rho = 0$ at $\rho = 0$ and $\rho = 1$ gives then the radial dependence of the temperature distribution. It is then assumed that this function of ρ can be added to the function of ζ in (15) to obtain a first approximation to the temperature profile in the insulated wall problem, thus:

$$\Theta_1 = \frac{(\alpha+4)^2}{8(\alpha+2)}\left(\rho^2 - \frac{2\rho^{\alpha+4}}{\alpha+4} + 4\zeta\right) \tag{16}$$

This approximate expression has been used for rough estimates of the effects of the viscous heat effects on the flow curves of plastics melts.[1]

GENERAL SOLUTION TO THE DIFFERENTIAL EQUATION FOR THE TEMPERATURE DISTRIBUTION

When the expression for θ_1 in either (14) or (16) is substituted into (13), which in turn is substituted into the differential equation (10), the following equation is obtained for θ_2 for **both** the isothermal and the insulated wall problems:

$$(1-\rho^{\alpha+2})\,\frac{\partial\theta_2}{\partial\zeta} = \frac{1}{\rho}\,\frac{\partial}{\partial\rho}\left(\rho\,\frac{\partial\theta_2}{\partial\rho}\right) \tag{17}$$

The functions θ_2 obtained from (17) are different for the isothermal and insulated wall problems, since the boundary conditions (11) and (12) are different for the two problems.

The solution to the partial differential equation (17) is of the form:

$$\Theta_2\,(\rho,\zeta) = -\sum_{i=1}^{\infty} B_i\,\phi_i\,(\rho)\,\psi_i\,(\zeta) \tag{18}$$

in which the B_i are constants and the $\psi_i\,(\zeta)$ and $\phi_i\,(\rho)$ are the solutions to the following ordinary differential equations:

$$\frac{d\psi_i}{d\zeta} + a_i\psi_i = 0 \tag{19}$$

$$\frac{1}{\rho}\,\frac{d}{d\rho}\left(\rho\,\frac{d\phi_i}{d\rho}\right) + a_i\,(1-\rho^{\alpha+2})\phi_i = 0 \tag{20}$$

in which the a_i are the eigenvalues of the problem, obtained when the solutions to (20) are required to satisfy the boundary conditions. The solution to (19) shows that ψ_i is proportional to $\exp(-a_i\zeta)$. The solution to (20) may be obtained by means of a power series development; this is done in the next section for the special case that $\alpha = 1$. It is easy to show that the solutions to (20) must satisfy the orthogonality condition:

$$\int_0^1 (1-\rho^{\alpha+2})\,\rho\,\phi_i\phi_j\,d\rho = 0 \qquad i \neq j \tag{21}$$

The final expressions for the temperature distribution are:

ISOTHERMAL WALL:

$$\Theta = \frac{1}{4}\,(1-\rho^{\alpha+4}) - \sum_{i=1}^{\infty} B_i\,\phi_i\,(\rho)\exp(-a_i\zeta) \tag{22}$$

INSULATED WALL:

$$\Theta = \frac{(\alpha+4)^2}{8(\alpha+2)}\left[\rho^2 - \frac{2\rho^{\alpha+4}}{\alpha+4} + 4\zeta\right] - \sum_{i=1}^{\infty} B_i\,\phi_i\,(\rho)\exp(-a_i\zeta) \tag{23}$$

From these solutions it can be seen that the approximate solutions obtained in the preceding section are just the limiting solutions for large values of ζ.

The constants B_i are determined by writing (22) and (23) for $\zeta = 0$, then multiplying by $\rho\phi_j\,(1-\rho^{\alpha+2})$ and integrating from 0 to 1. In that way the following expressions are obtained:

ISOTHERMAL WALL:

$$B_j = \frac{1}{4}\,\frac{\displaystyle\int_0^1 (1-\rho^{\alpha+4})\,(1-\rho^{\alpha+2})\,\rho\,\phi_j\,d\rho}{\displaystyle\int_0^1 (1-\rho^{\alpha+2})\,\rho\,\phi_j^2\,d\rho} \tag{24}$$

INSULATED WALL:

$$B_j = \frac{(\alpha+4)^2}{8(\alpha+2)}\,\frac{\displaystyle\int_0^1 \left(\rho^2 - \frac{2\rho^{\alpha+4}}{\alpha+4}\right)(1-\rho^{\alpha+2})\,\rho\,\phi_j\,d\rho}{\displaystyle\int_0^1 (1-\rho^{\alpha+2})\,\rho\,\phi_j^2\,d\rho} \tag{25}$$

The solutions (22) and (23) for newtonian flow ($\alpha = 0$) were obtained by Brinkman[6]. Subsequently Lauwerier[14] showed that the solution for newtonian, isothermal-wall flow could be written in terms of Whittaker functions and that it is possible to obtain an asymptotic development to find the higher eigenvalues. In the next section a summary is given of the numerical results for the particular non-newtonian flow given by $\alpha = 1$. This choice for the index α corresponds to certain types of molten polyethylene plastics.

CALCULATION OF THE TEMPERATURE PROFILE (FOR $\alpha = 1$)

The eigenfunctions ϕ_i which satisfy the differential equation (20) for the particular case $\alpha = 1$ can be written as the series:

$$\phi_i = \sum_{k=0}^{\infty} b_{ik}\,\rho^k \tag{26}$$

in which b_{i0} is arbitrarily chosen to be unity. The coefficient b_{i1} must be zero to satisfy the boundary condition at $\rho = 0$. Substitution of (26) into the differential equation (20) gives the following recursion formula for the b_{ik}:

$$b_{ik} = -(a_i/k^2)\,(b_{i,k-2} - b_{i,k-5}) \tag{27}$$

Hence all of the b_{ik} can be expressed in terms of the eigenvalues a_i

$$b_{i2} = -(1/4)a_i,\ b_{i3} = 0,\ b_{i4} = (1/64)a_i^2,\ b_{i5} = (1/25)a_i,\ b_{i6} = (1/2304)a_i^3,\ b_{i7} = -(29/4900)a_i^2,\ \text{etc.}$$

The eigenvalues a_i are determined from the boundary conditions at $\rho = 1$ which require that:

[13] *M. Reiner, "Deformation and Flow," Lewis, London (1949)—Chapter 6 and 7.*

[14] *H. A. Lauwerier, Appl. Sci. Res., A2, 184-204 (1951)*

ISOTHERMAL WALL: $$\sum_{k=0}^{i} b_{ik} = 0 \qquad (28)$$

INSULATED WALL: $$\sum_{k=0}^{i} k\, b_{ik} = 0 \qquad (29)$$

The values of the a_i found herefrom are listed in Table 1. With these a_i the b_{ik} can be calculated from (27) and thereby the eigenfunctions are obtained from (26); the first three eigenfunctions are tabulated in Table 2. The coefficients B_i are then obtained from (24) and (25); the values calculated by numerical integration are given in Table 1.

TABLE 1

Constants for Use in Equations (22) and (23) when $\alpha = 1$

i	Isothermal Wall		Insulated Wall	
	a_i	B_i	a_i	B_i
1	6.582	+0.349	0	+0.3125
2	39.09	−0.147	22.43	−0.413
3	99.50	+0.057	73.10	+0.146
4	187.9	−0.014*	151.9	−0.040*

*Estimated values

TABLE 2

The Eigenfunctions ϕ_i for Use in Equations (22) and (23) when $\alpha = 1$

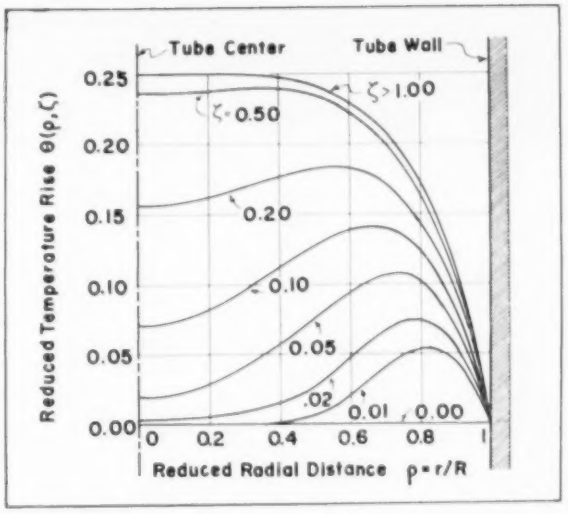
ρ	Isothermal Wall			Insulated Wall	
	ϕ_1	ϕ_2	ϕ_3	ϕ_2	ϕ_3
0.0	1.000	1.000	1.000	1.000	1.000
0.1	0.988	0.884	0.766	0.945	0.825
0.2	0.935	0.646	0.228	0.789	0.393
0.3	0.855	0.310	−0.257	0.550	−0.079
0.4	0.756	−0.045	−0.403	0.290	−0.369
0.5	0.638	−0.311	−0.209	0.020	−0.385
0.6	0.506	−0.414	0.118	−0.187	−0.181
0.7	0.374	−0.385	0.310	−0.340	0.082
0.8	0.236	−0.293	0.310	−0.434	0.295
0.9	0.110	−0.166	0.185	−0.469	0.375
1.0	0.000	0.000	0.000	−0.470	0.390

Note: ϕ_1 for the insulated wall problem is 1.000 for all values of ρ.

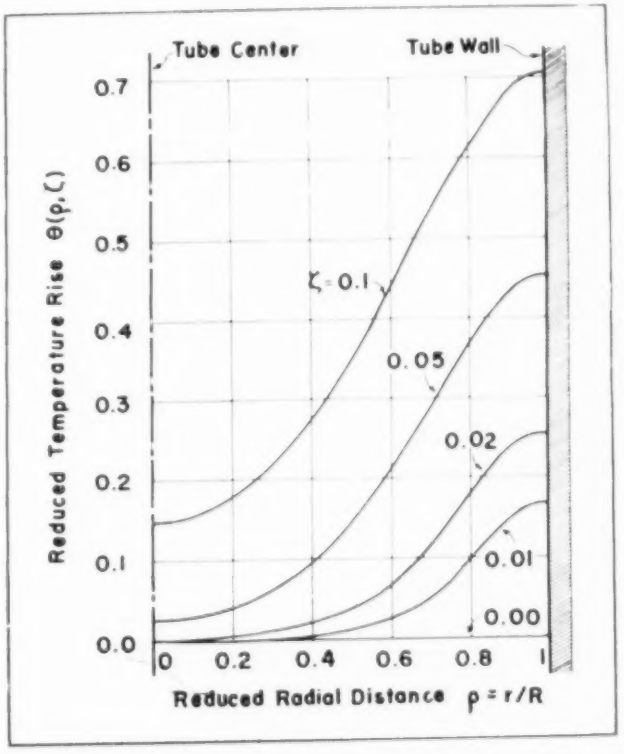
From the expressions given in (22) and (23) for the temperature distribution and the information given in Tables 1 and 2, one can compute the temperature profiles as a function of the reduced axial distance ζ. Figures 2 and 3 show graphically the development of the temperature distribution in both isothermal wall and insulated wall flow. These curves allow one to specify the value of ζ for which the exponential terms in (22) and (23) have virtually ceased to contribute to $\phi(\rho, \zeta)$—that is, a "Temperatureinlauflaenge," ζ: for the isothermal wall $\zeta_T = 1$ and for the insulated wall $\zeta_T = 0.02$. The average temperature and the maximum temperature for each of the temperature profiles are given in Table 3.

ESTIMATION OF EFFECT ON VISCOUS HEAT PRODUCTION ON FLOW CURVES

For shear stresses up to about $\tau_R = 10^6$ dyn cm^{-2} many plastics melts behave according to the power law (9), so that rheometer extrusion data are usually represented by straight lines on plots of log τ_R vs. log D (in which $D = 4Q/\pi R^3$, the quantity being the volumetric discharge rate from the capillary). Measurements by Tordella[15] on an "Alathon" 1 polyethylene resin above $\tau_R =$



2. Profiles of the reduced temperature for a fluid with $\alpha = 1$ flowing in a tube whose walls are maintained at constant temperature.



3. Profiles of the reduced temperature for a fluid with $\alpha = 1$ flowing in a tube with thermally insulated walls.

10^6 have shown deviations from the linear flow curves. One such curve is reproduced in Figure 4. In this section we obtain an approximate expression for the deviation from the linear flow curves in terms of the viscous heat effects.

It is first necessary to specify the temperature variation of the viscosity. For this purpose we modify (4) by writing:

$$\frac{1}{\mu} = \frac{1}{\mu^0}\left(\frac{\tau_R}{\tau^0}\right)^a \left(\frac{r}{R}\right)^a (1 + \beta_1 T + \beta_2 T^2) \qquad (30)$$

in which T is the temperature rise above T^0 the tempera-

[15] *J. P. Tordella, Society of Plastics Engineers Journal, 9, 6-9 (1953)*

ture of the stock solution entering the capillary, and μ^0 is that value of the (true) viscosity at the standard shear stress τ^0 and at the stock temperature T^0; the quantities β_1 and β_2 are constants characteristic of the melt. A quadratic dependence of the fluidity on temperature is adequate over a range about 200°C.

An expression may now be obtained for the velocity distribution by integrating (1), using the expresion for μ given in (30) and the expresions for the temperature profile in (22) and (23). This gives a modification of (5):

$$v = \frac{R}{\alpha+2}\frac{\tau^0}{\mu^0}\left(\frac{\tau_R}{\tau^0}\right)^{\alpha+1}\left[(1-\rho^{\alpha+2})+(\alpha+2)\beta_1 K\int_\rho^1 \rho^{\alpha+1}\theta\,d\rho + (\alpha+2)\beta_2 K\int_\rho^1 \rho^{\alpha+1}\theta^2\,d\rho\right] \quad (31)$$

in which $K = 4R^2\tau_R^{\alpha+2}/(\alpha+4)^2\lambda\mu^0\tau^{0\alpha}$

The velocity distribution so obtained is a function of both the radial and axial cordinate, in contradiction to assumption 5 above. Therefore this expression for the velocity distribution must be regarded as an approximation.

The function $D(\tau_R)$, which gives essentially the flow rate as a function of the pressure, is then obtained from the velocity distribution thus:

$$D = (4Q/\pi R^3) = (4/\pi R^3)\int_0^R v\,2\pi r\,dr = (8/R)\int_0^1 v\,\rho\,d\rho \quad (32)$$

in which v is the velocity distribution at z = Z. Substitution of 31 into (32) then gives the result:

$$D/D_0 = 1 + V_1W_1\tau_R^{\alpha+2} + V_2W_2\tau_R^{2(\alpha+2)} \quad (33)$$

in which $D_0 = 4\tau_R^{\alpha+1}/(\alpha+4)\mu^0\tau^{0\alpha}$

is the flow rate which the fluid would have in the complete absence of viscous heat effects; and the V's and W's are:

$$V_1 = \frac{4R^2\beta_1}{(\alpha+4)(\mu^0\tau^{0\alpha})\lambda} \quad (34)$$

$$V_2 = \frac{16R^4\beta_2}{(\alpha+4)^3(\mu^0\tau^{0\alpha})^2\lambda^2} \quad (35)$$

$$W_1(\zeta_Z) = \int_0^1 \rho^{\alpha+3}\theta(\rho,\zeta_Z)\,d\rho \quad (36)$$

$$W_2(\zeta_Z) = \int_0^1 \rho^{\alpha+3}[\theta(\rho,\zeta_Z)]^2\,d\rho \quad (37)$$

in which ζ_Z is the value of ζ for z = Z. Hence the deviation from isothermal behavior can be expressed simply in terms of the shear stress at the wall τ_R, the factors V_1 and V_2 (which contain all the physical constants of the fluid), and the weighted integrals over the temperature distribution W_1 and W_2. The latter quantities are tabulated in Table 3, for $\alpha = 1$.

§7. Numerical Results for a General Purpose Polyethylene Melt ($\alpha = 1$)

In order to compare the theoretical results with the experimental flow curve of Tordella[15] shown in Figure 4, the following numerical values were used:

Dimensions of Apparatus

R = radius of capillary tube = 0.0394 cm

Z = length of capillary tube = 1.28 cm

Standard Quantities

T^0 = standard (stock) temperature = 190 C

τ^0 = standard shear stress = 10^5 dym cm^{-2}

Physical Properties of Melt

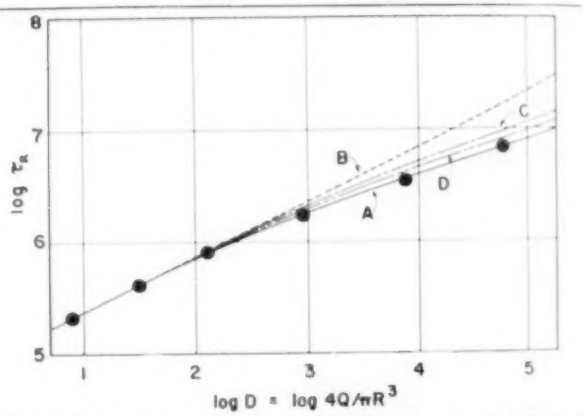
c = specific heat per unit volume* = 0.43 cal cm^{-3} deg^{-1}

λ = thermal conductivity in neighborhood of $T^{0\,\dagger}$ = 10^{-4} cal cm^{-1} sec^{-1} deg^{-1}

μ^0 = (true) viscosity at τ^0 and T^0 † = 4.76 x 10^6 gm cm^{-1} sec^{-1}

α = index for power law shear stress dependence of viscosity † = 1.0

(Please turn to page 53)



4. Experimental and theoretical flow curves for an "Alathon" I Polyethylene resin at 190°C. The four curves shown are: (A) Experimental curve with points measured by J. P. Tordella[15] at 190°C. (B) Extrapolation of experimental curve based on experimental data at shear stress less than 10^6. (C) Calculated flow curve based on "insulated" model for capillary flow. (D) Calculated flow curve based on "isothermal" model.

TABLE 3

Tabulation of θ_{av}, θ_{max}, W_1 and W_2 for Non-Newtonian Fluids with $\alpha = 1$

	Isothermal Wall				Insulated Wall			
	θ_{av}	θ_{max}	W_1	W_2	θ_{av}	θ_{max}	W_1	W_2
0.001*	0.007	0.022	0.0026	0.000039	0.010	0.048	0.0044	0.00014
0.002*	0.011	0.029	0.0035	0.000072	0.018	0.070	0.0072	0.00035
0.005*	0.018	0.042	0.0052	0.000165	0.039	0.115	0.0140	0.00122
0.01	0.028	0.056	0.0071	0.000308	0.071	0.171	0.0230	0.00318
0.02	0.043	0.074	0.0097	0.000570	0.126	0.258	0.0384	0.00827
0.05	0.073	0.108	0.0141	0.00121	0.275	0.455	0.0752	0.0299
0.1	0.104	0.140	0.0177	0.00194	0.498	0.707	0.124	0.0792
0.2	0.140	0.183	0.0212	0.00290	0.922	1.14	0.211	0.225
0.5	0.173	0.239	0.0245	0.00398	2.17	2.40	0.461	1.066
1.0	0.179	0.250	0.0250	0.00417	4.25	4.48	0.878	3.85

*The values of θ_{av}, θ_{max}, W_1 and W_2 for $\zeta < 0.01$ were obtained by extrapolation using an exponential function of the form $A\exp(-B\zeta)$.

Dynamic Investigations on Polymeric Solutions Using Different Methods

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and

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Dynamic testing of polymer solutions has been performed over a number of years by several laboratories using completely different experimental procedures. Up to now, it has not been possible, except in the case of the solid polyisobutylene in the Bureau of Standards' cooperative program, to ascertain whether these different methods give the same values for the same polymer solutions; in other words, whether the values of the dynamic modulus, G' and the loss modulus, G'' are real constants of the material and therefore independent of experimental method. We are reporting here a series of measurements made on a single solution, 20% NBS polyisobutylene in decalin, using three completely different methods in two laboratories, all having the stressing in **shear** in common:

1. Ferry's method of wave propagation as described[1] in 1941 and 1942. These measurements were performed at the Department of Chemistry, University of Wisconsin.
2. A method used at The Franklin Institute for measuring the force and displacement together with the phase angle, using an oscilloscope and double transducers as described[2] in 1953 by W. Philippoff.
3. A modification of Mason's method[3] of the vibrating torsion crystals as described by Rouse and Sittel[4] at the Franklin Institute.

Whereas Ferry's method does not involve any assumptions except the measurement of the velocity of the shear wave and its attenuation by **optical** means in an infinitely extended solution, the transducer method of Philippoff involves a more complicated velocity distribution, and calibration of the force and deformation in absolute units. The measurement is basically **mechanical.** By the method of Sittel, the impedance offered by the solution to a crystal vibrating at resonance is measured **electrically.** The frequency range and the maximum amplitude obtainable by these methods are seen in Table I.

TABLE 1

Method	Frequency Range (cps)	Maximum Shear Amplitude (cm)
Ferry (1)	100 to 800	<5
Philippoff (2)	3×10^{-3} to 10	130
Sittel (3)	20×10^{3} (not a "range")	10^{-5}

The experiments at the University of Wisconsin were part of a program of research on the physical structure and properties of cellulose derivatives and other polymers, supported by the Allegany Ballistics Laboratory, Cumberland, Maryland, an establishment owned by the United States Navy and operated by the Hercules Powder Company under Contract NOrd 10431.

[1] *J. D. Ferry, Rev. Sci. Instr., 12:79 (1941). T. Amer. Chem. Soc. 64 1323, 1330 (1942).*
[2] *W. Philippoff, J. Appl. Phys., 24:685 (1953).*
[3] *W. P. Mason, Trans. Am. Soc. Mech. Eng., 69:359 (1947).*
[4] *P. E. Rouse and K. Sittel, J. Appl. Phys., 24:690 (1953).*

It is obvious that the range of amplitudes is nearly 1 to 10^7. Using the results reported by Philippoff[2], one could expect the dynamic properties to be independent of amplitude. This cooperative work allows us to prove it in an otherwise inaccessible range of 1 to 10^7. Only if the properties are independent of the amplitude, could one expect the results from these three different methods to fit the same curve. The evaluation of the complex shear modulus (G) in dynes/cm², the phase angle (δ) between force and deformation, as well as the real part of the shear modulus (G') and the loss modulus G'' (= viscosity $x\omega$) is given in the next paragraph. (ω is the circular frequency of vibration.)

RESULTS WITH METHOD 1

Ferry's method of wave propagation is evaluated according to the equations: [5]

$$G' = \rho \lambda^2 f^2 \frac{4\pi^2 [4\pi^2 - (\lambda/X_\alpha)^2]}{[4\pi^2 + (\lambda/X_\alpha)^2]^2}$$

$$G'' = \rho \lambda^2 f^2 \frac{16\pi^3 (\lambda/X_\alpha)}{[4\pi^2 + (\lambda/X_\alpha)^2]^2}$$

where ρ is the density of the solution in g/cc
λ is the wave length in cm
f is the frequency in cps
X_α is distance in direction of propagation when amplitude decreases 1/e = 0.368

The measurements were made at 0, 25 and 50 C. Measurements of the wave length are essentially much more exact than the measurements of attenuation (X_α) so that the measurements of G' have much smaller errors than those of G''.

RESULTS WITH METHOD 2

Measurements made with the double transducer are evaluated by calibrating the oscillographically recorded hysteresis loop in **kg** and microns (μ) and evaluating the loop directly as double as maximum force (=2F) in **kg**, double the maximum displacement (2D) of the inner cylinder in μ, and breadth of the ellipse **a** at D = 0. The measurement thus gives, in the well known way,

$$G = 0.715 \times 10^6 \, F/D \text{ dynes/cm}^2$$

and $$\sin \delta = a/2F$$

The instrument used for these measurements has not been described in detail. It consists of concentric cylinders where the inner cylinder is axially displaced with respect to the outer one by an amount $\pm$ **D** and the liquid is pumped through the annulus as the inner cylinder moves up and down. The formulas involved have been developed by a series of investigators, for example Smith, Ferry, and Schremp.[7] The inner diameter of the outer

[5] *W. Philippoff, J. Appl. Phys., 25:1102 (1954).*
[6] *J. D. Ferry, W. M. Sawyer and T. N. Ashworth J. Polymer Sci. 2:593 (1947).*

cylinder was 77.80 mm and the outer diameter of the inner cylinder 65.00 mm. The equivalent length of the inner cylinder determined by calibration was 47.00 mm. The constant 0.715 x 10^6 is calculated from the geometrical dimensions and checked by calibration with a Bureau of Standards normal oil of about 200 P viscosity at 25°C. The maximum shear at the well of the inner cylinder is $\gamma = 0.00287\ D$ with D in microns. The measurements were performed at temperatures of +25, 0, and −23°C. Higher temperatures were not used as a noticeable change in concentration due to evaporation of the solvent was observed in preliminary runs.

RESULTS WITH METHOD 3

Mason's method of the torsion crystal was recently applied to solid rubber samples by K. Sittel and P. Rouse.[4] Sittel has shown[5] that it can also be adapted for measurements on viscous solutions by placing the end of the crystal (10 mm diameter and 100mm length) in the plane surface of the liquid. As long as the wave length of the shear wave in the liquid is only a small fraction of the diameter of the crystal, the shear wave can be considered as a plane wave traveling in an infinite medium. In other words, the wave propagation is assumed to be confined within a cylindrical volume which extends from the crystal into the liquid. The crystal touching the surface of the test liquid is unsymmetrically loaded. It can be shown that this has a negligible influence on the wave distribution along the crystal so that the results can be treated in the same fashion as in the case of a symmetrically loaded crystal if the change of the resonant frequency and the electrical resistance between the loaded and unloaded crystal is doubled.

When the crystal is brought in contact with the liquid, the liquid will not only wet the flat end surface, but due to the surface tension, rise on the sides of the crystal. This capillary rise of a liquid at the circumference of a cylinder is derived by Ferguson[6], assuming complete wetting. In our measurements, the capillary rise of the solution has been experimentally determined; it was approximately 0.025 cm ten minutes after contact with the viscous solution was first established, and approached a height of 0.13 cm after approximately two hours. In the case of the solvent the liquid rose almost instantly to its final height of 0.13 cm. As the height is only a small percentage of the crystal length, the effect is merely to add a load to the end of the crystal without taking into account the change of the wave distribution along the crystal. To establish a definite contact angle and thereby decrease the height of rise of the liquid, a thin film of silicone grease was applied in a narrow ring around the end of the crystal. To avoid errors due to the less-than-perfect crystal surface, the crystal was calibrated with a Bureau of Standards normal oil (viscosity = 0.7223 poise, and density = 0.8682 g/cm³ at + 25°C) in the same way as described above. Then the solvent and the solution were measured consecutively. The capillary rise of the liquids was determined each time and all measurements reduced to the contact with one flat end.

The measurements are evaluated according to the equations:

$$G' = \frac{1}{\rho_c} \frac{L^2 \rho_c^2}{4} \left[\frac{\Delta R^2}{4 L_e^2} + \Delta \omega^2 \right]$$

$$\tan \delta = \frac{1}{L_e} \frac{\Delta \omega \Delta R}{(\Delta R^2 / 4 L_e^2) - \Delta \omega^2}$$

where ρ_c is the density of the solution in g/cm³
ρ_c the density of the crystal in g/cm³
L the length of the crystal in cm
L_e the effective inductance of the crystal, which is determined experimentally in henrys
ΔR the difference in the electrical resistance between the loaded and unloaded crystal in cm
$\Delta\omega = 2\pi\ \Delta f$ where Δf is the difference in the resonant frequency between the loaded and unloaded crystal in cps.

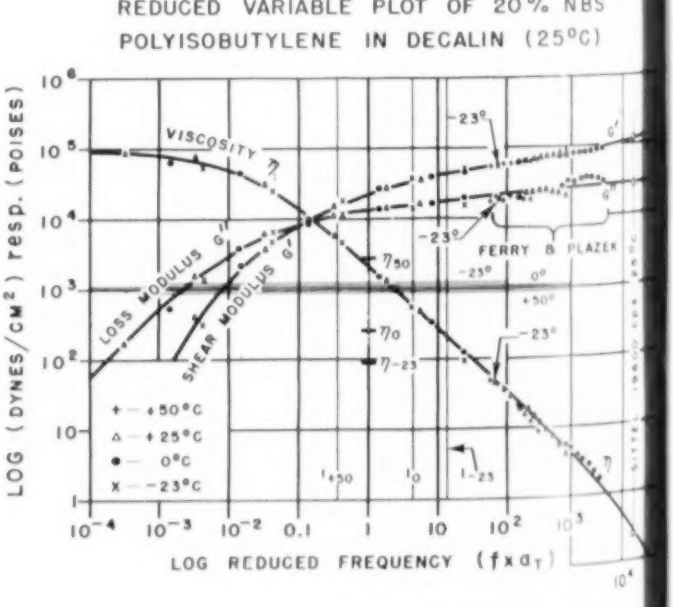
We see that under these conditions (plane wave and infinite sample), the dimensions of the specimen do not enter into the calculations, and only electrical quantities and Δf are used. In contrast to the low-frequency measurements these measurements were made at very low shearing strains. The shear strain can be easily calculated from the resistance of the loaded crystal, the applied voltage and the attenuation constant. For the solution under test, the maximum shearing strain $15\ \gamma = \frac{d(r\theta)}{dZ}$

r being the radius of the crystal, θ the maximum angular displacement, and Z the axial coordinate, $\gamma = 1.0 \times 10^{-5}$. Measurements were made at 19.6 kc and 25°C. For these conditions the dynamic shear modulus of the 20% isobutylene solution was determined as $G' = 1.3 \times 10^7$ dynes/cm² tan $\delta = 0.21$.

COMPARISON OF RESULTS

The results of the measurements with the three different methods are shown in Figure 1. The measurements at different temperatures were reduced by the a factors of Ferry to form a single reduced curve at 25°C. As can be seen from the measurements, the lowest frequency in method 2 was measured at 25 C. Sittel's measurements were also made at 25 C so that the curves give the dispersion of the mechanical properties of the solution in the range of about eight decades of frequency under isothermal conditions. The values at other temperatures fit in nicely with these measurements at 25°C. The G' curve is continuous in the whole range investigated.

The conclusions to be drawn from the curve are ob-



[4] *T. L. Smith, J. D. Ferry and F. W. Schremp J. Appl. Phys., 20 144 (1949).*
[5] *K. Sittel, to be published.*
[6] *Wien Harms, Handbuch d. Exp. Physik., Vol. VI, 1928, p. 95.*

vious. The dynamic properties of the solution are the real constants independent of the method used. The amplitude has no measurable influence on the values even over a range of nearly 1 to 10^7. The general shape of the curve is similar to the one obtained by Ferry for other polymer solutions. (See also W. Philippoff[9].) Especially important is the merging of the viscosities at low frequencies to a constant value; the same value of 80,000 poise being measured with a rotational viscometer at very low rates of shear below 0.003 sec^{-1} (see W. Philippoff[5]). The validity of Ferry's method of reduced variables is again demonstrated under the most stringent conditions possible; namely, fitting an isothermal curve in the whole range of frequencies with measurements at higher and lower temperatures.

These results add substantial weight to the dynamic experiments, proving that the dynamic modulus and viscosity are in fact constants of the material.

[9] *W. Philippoff, Physikalische Zeitschr. 35:900 (1934).*

SUMMARY

These measurements show that a determination of the dynamic properties of polymer solutions could be obtained in eight decades of frequency at one temperature. Amplitude effects if present are of minor importance. The three completely different methods compared give absolute values entirely comparable with each other. Ferry's method of reduced variables has been substantiated.

Low Shear Viscometry....

THE SQUIBB VISCOMETER

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The need for an instrument capable of providing low shear viscometry of suspension has been felt in studies on the structural features of printing inks (6) and caking tendencies of certain pharmaceutical dispersions which are underway in this laboratory. Interest has centered on the determination of the importance of secondary structures using a combination of both low shear and high shear data as detailed by Reiner (8).

In the case of the pharmaceuticals, which in this case were aqueous dispersions, high shear data have been obtained by means of a pressurized capillary viscometer; low shear data could not be obtained with any existing fundamental instrument. Therefore, a rotational viscometer was designed for the low shear range. The versatility of the instrument is illustrated herein for systems ranging from water to printing inks and at shear rates from 1 to 600 reciprocal seconds.

The detection of secondary structures is only one of several uses of rotational viscometry. Reiner (8) has provided a list of criteria of structural features of fluid dispersions, among which are the temperature dependence of the relative viscosity and peculiarities in the shape of the flow curve. These measurements have been taken by various workers (5, 6) on dispersions at moderate shear rates, but information on low shear anomalies has not yet been reported to an extent which permits an interpretation of the mechanism of the anomaly.

Many of the reported studies have failed to yield significant data because they have been conducted at shear rates at which much of the structure of the dispersion has been broken down. One of the first instruments for the measurement of stresses at extremely low shear rates was constructed by Hayden and Schweyer (4), and adapted to materials of low viscosity by Bergen and Patterson (3); however, these instruments suffered from end effects which introduced variable shear rates and reduced their effectiveness in the measurement of non-Newtonians.

Numerous details of the final design of the viscometer were supplied by the Squibb Institute for Medical Research, a division of the Olin Mathieson Chemical Company. The prototype used in this research was constructed by the Squibb Institute.

DESIGN OF THE VISCOMETER

1. **Schematic Diagram** — A diagram of the essential features of the viscometer is given in Figure 1. The heart of the instrument is a calibrated wire which is gripped by a pin vise at its lower end and by torsion head at the upper extremity. At the midpoint of the wire is attached a stainless steel seamless ring equipped with three tabs attached by radial connecting arms to a pin vise concentric with the ring. The pin vise is clamped to the torsion wire at a point halfway between the torsion head and the lower pin vise of the frame, thus providing a free suspension which can be rotated or oscillated without friction since no bearings are employed in the torque measuring assembly.

In the model used in this investigation the wire span is 27 inches; the ring diameter is 1.3 inches and its height is 0.5 inch.

A double-walled cup concentric with the wire and the ring can be raised so that the ring fits into the annular-space of the cup. Arrangements of this type, called bell-trough assemblies, have been reported previously (1, 7, 9); however, their use in connection with the completely frictionless suspension is necessary in order to allow the wire to pass completely through the shear assembly. Three cups are currently in use, providing clearances ranging from 0.03 to 0.20 inches.

The longer torque arm and the existence of shear on both walls of the ring increase the sensitivity of the viscometer, while the provision for taking all readings at the same positioning of the ring alleviates the condition common to rotational viscometers of a variable sensitivity dependent on the angular displacement of the torsion member. End effects are virtually absent because they have been confined to the thickness of the shell rather than to its entire diameter as in conventional rotational viscometers. These second order end effects are further minimized by equipping the ring with the tabs mentioned above which elevate the points of attachment to the pin vise above the liquid level.

2. **Details of Operation** — A wire of suitable torsion constant is fixed to the tension gage and then threaded through the torsion head, the ring and pointer, and the cup, and is finally clamped in the pin vise which is lowered until the wire is at the desired tension. After the torsion head is set at zero and clamped to the wire, the ring and cup are carefully positioned by means of an adjustment on the lower pin vise; the final zero adjustment is made by moving the target in line with the pointer.

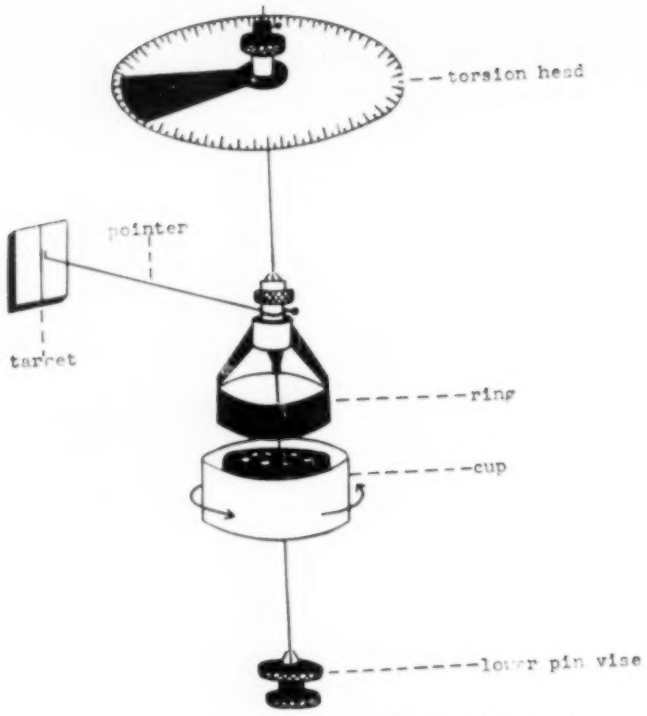

Figure 1. A schematic drawing of the Squibb viscometer

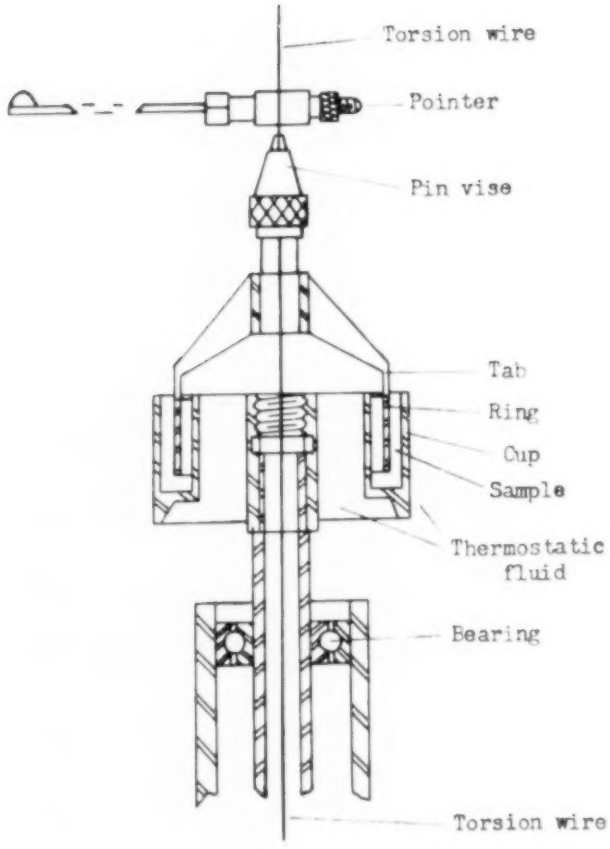

Figure 2. Scale drawing showing detail of the viscometer.

The ring immersion is adjusted to a reproducible value by means of a depth indicator supplied with the instrument. The cup is filled with the liquid or dispersion and is set into motion at a rate which is read on the tachometer. A couple is transmitted through the fluid to the ring which is thereby caused to rotate until the couple exerted on the wire equals the restoring couple due to the rigidity of the wire. The tangential force acting on the ring is calculated from the angle θ through which the torsion head vernier has to be turned to bring the pointer back to the zero mark, together with a knowledge of the wire constant and the dimensions of the ring.

The cup is operated at rotations from 1.0 to 660 rpm by means of a variable speed dc motor. Features of the viscometer which are not shown in the schematic diagram, Figure 2, include the constant temperature bath, a tension gage to ensure that the wire is always under the tension at which it was calibrated, a depth gage for adjusting the ring immersion, and a tachometer.

The use of cups of different dimensions and wires of different torsion constants enables flow curves to be obtained for a wide variety of systems. Systems ranging in viscosity from 0.01 to over 200 poises have been investigated extensively at shear rates from 1.0 to about 100 sec^{-1}, with occasional extensions to higher rates.

EVALUATION OF THE INSTRUMENT

1. Wire Calibration — The measured quantities in rotational viscometry are the revolutions of the cup per unit time and the deflection of the torsion member. Three methods of determining the torsion constants of the wires and filaments used in the present study are described below.

a. Direct measurement of torque. With the viscometer lying on its side so that the torsion wire is horizontal, the pointer can be used as a lever for the application of known torques by means of small weighted segments of chromel wire.

b. Indirect measurement of torque. The inconvenience of laying the viscometer on its side during calibration has led to the use of the method described by Barr (2) in which a thread supporting a balance weight is deflected from the vertical by the pointer. In the Squibb viscometer the deflection is brought about by adjustment of the torsion head whose reading is equated to the horizontal component of the weight.

c. Oscillation period. The third method is based on the timing of the period of oscillation of the ring as it hangs free of the cup and its contents. A similar alternative is to use a torsion bob of known moment of inertia (2) in order to secure absolute values of the torsion constant rather than the relative values obtained by the use of the ring.

The calibration results from the oscillation method agree most closely with the calibrations supplied with certain of the wires by the MacMichael Company and with the rated viscosities of a group of standard oils used in the extensive calibration of the various wires.

2. Instrument Calibration — The precision of the instrument was evaluated by four operators working independently, using a Hartman-Leddon 14.1 cp. standard oil. When Cup #1 (trough width 0.24 cm, 4.0 ml capacity) was used in conjunction with a wire whose torsion constant was 23.1 dyne cm/deg, the average viscosity was determined by the four observers to be 13.4 with the precision extremes listed in Table 1.

Table 1. Calibration of Viscometer at 25°C Using 14.1 cp Oil. Cup #1

Operator	Shear Range (Sec^{-1})	No. of Detns.	η Avg.	Standard Deviation of S
1	12.5 to 118	18	13.7	0.21
2	9.0 to 108	15	13.1	0.15
3	5.0 to 108	19	13.1	0.15
4	7.7 to 120	17	13.7	0.26
		Avg.	13.4	0.19

For calculation of the precision, the slope of the best line through all of the points was taken as the true viscosity, while the intercept which was finite in each case was considered to be a natural outcome of the least squares technique in which equal weight was attached to all the points. Deviations in S appeared to be the same at low shear rates as at high ones.

The accuracy of the viscometer can be seen to be affected by a negative bias of 0.7 units in the slope which was 13.4 instead of 14.1 in this case; the error, therefore, is 5%. This figure probably can be lessened by machining the cup and ring to closer tolerances or by improved alignment.

The problem of cup-ring alignment was the most serious one encountered in calibrations using the small cup, but disappeared when a cup of larger capacity, (Cup #2: 9.5 ml, 0.556 cm), was used. In this case, small departures from concentricity produced negligible effects on the flow curve.

Water was the first calibration fluid to be studied, with the result that a Newtonian flow curve was obtained at shear rates up to about 100 sec^{-1} in Cup #1. After about 50 sec^{-1}, a pronounced increase in slope occurred with Cup #2. The most likely cause is the onset of turbulence which has been calculated from Taylor's formula (2) to lie near the 50 sec^{-1} value found experimentally. Without doubt, turbulence is one of the most serious limitations to the use of the Squibb viscometer but fortunately the problem is confined to a highly specific set of conditions: namely, the high shear rate viscometry of materials of low viscosity in a cup of relatively large clearance. Since the attainment of high shear rates is more readily accomplished in cups of small clearance, this combination of requirements is not likely to be encountered.

EVALUATION OF TYPICAL SYSTEMS

1. Aqueous Procaine Penicillin Suspension — The proprietary formulations of procaine penicillin are aqueous dispersions whose vehicle, or menstrum, is a complex mixture of soluble and micellar ingredients formulated to be compatible with body fluids. The menstrum for the system reported here had a viscosity of 2.7 cp as determined by both the Ostwald and Squibb viscometers. Other commercial menstrums have viscosities which depart from this figure by a wide margin. In each case, a knowledge of the menstrum viscosity under the conditions of shear encountered in the Squibb viscometer was necessary in order that relative viscosities of the blends could be calculated.

In the case of the procaine penicillin suspensions, however, a considerable amount of interest centered around the shape of the flow curves of fresh and aged samples.

In general the blends were Newtonian. As in the case of the menstrums, the range of viscosities encountered with 30% procaine penicillin blends (300,000 units/ml) made in the laboratory and purchased on the open market was enormous. Variations from 4 to 60 cp were not uncommon. Most of this variation arose from differences among menstrums.

One important finding which low shear viscometry produced was in connection with the measurement of the viscosities of a selected 30% blend at 15° and 35°. During this temperature interval the viscosity in the range from 1.0 to 50 sec^{-1} dropped only from 17 to 16 cp.; in this same span of temperature the viscosity of water is reduced by 30%. Since the relative viscosity increases with temperature, an application of Reiner's criterion (8) causes one to arrive at the conclusion that competition exists between orientation of the particles due to flow and randomness as caused by Brownian movement. That this explanation can account for all of the measured effect is doubtful; at any rate, the kinetic unit of the dispersion appears to be larger at the higher temperature.

The Squibb viscometer was useful in observing the rate of an apparent buildup of a thixotropic structure after a procaine penicillin blend has been worked at a high rate of shear. This behavior is apparent from Figure 3, which shows the rise in measured viscosity with time after the suspension had been subjected to shear rates above 10,000 sec^{-1} before the start of the test. Buildup curves of this nature have been duplicated several times by the simple expedient of filling the viscometer by means of a hypodermic needle, during which operation the high shear rate is achieved. The phenomenon shown in Figure 3 has been explained satisfactorily only in terms of the breakdown in structure suffered by the suspension as it passes through the needle and its subsequent buildup in the viscometer. An observation of this nature requires the application of a low shear rate (11 sec^{-1} in this case).

2. Calcium Carbonate Dispersions — Another field of investigation in which the Squibb viscometer has been of value is in the viscometric analysis of model systems for printing inks. One series of dispersions was formulated by milling precipitated calcium carbonate with Triol 230, a 29 poise branched trihydroxy ether produced by Carbide and Carbon Chemicals Co., and diluted with water to increase the polarity; a comparison series was made using a 5 poise polybutene, a non polar hydrocarbon of molecular weight 250-300 provided by Indoil Chemical Co. The chief difference between the two systems is that the polybutene dispersions produced flow curves with an extensive linear range, while the flow

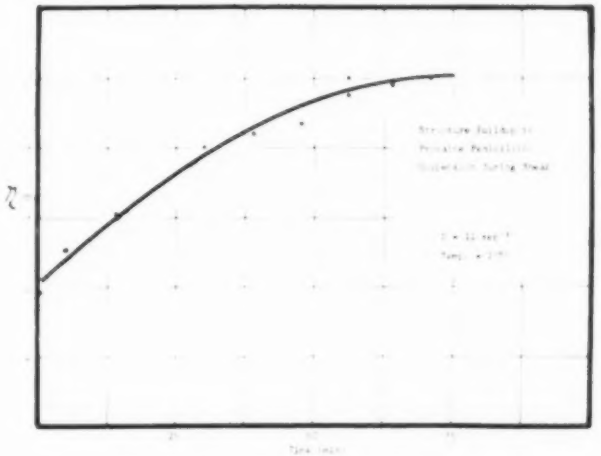

Figure 3. Structure buildup during shear.

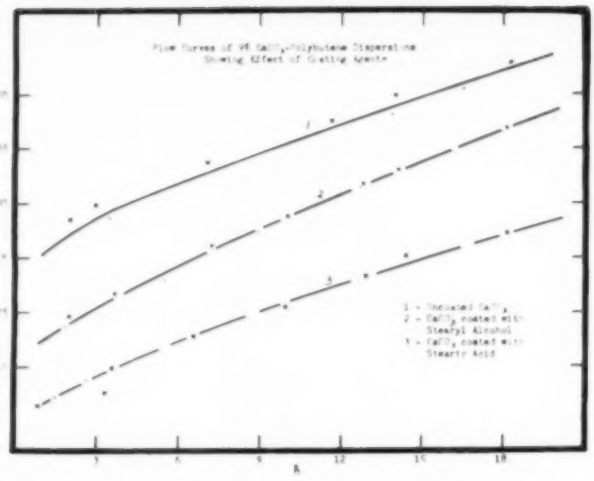

Figure 4. Flow curves show effect of coating agents.

curves for the Triol dispersions were curved throughout the entire range studied.

A significant finding of this use of the Squibb viscometer is that the full impact of particle-vehicle interactions was not realized until precision measurements in the low shear range were made. For example, in the coating of calcium carbonate by stearyl alcohol (reversible) and stearic acid (irreversible), no appreciable changes in the shape of the flow curves were observed when the rheological data were taken on a conventional rotational viscometer designed for a shear range up to 600 sec^{-1}. When the Squibb viscometer was employed, the three curves became distinct and showed evidences of structure in the low shear range; examples of this spread are shown in Figure 4. The amount of thixotropy was reduced by the coatings (6).

In contrast with numerous difficulties encountered with the aqueous systems, the flow curves obtained with the calcium carbonate systems exhibited no hysteresis (6) and were of sufficient precision to permit conclusions to be drawn concerning the effects of the polarity of the medium and of surface coatings on the carbonate; they will be published at a later date.

Although it was designed for fluid dispersions, the Squibb viscometer has proved to be versatile enough to be used in obtaining flow curves of printing inks whose viscosities range to over 200 poises. The practical limit to the consistency of materials which can be investigated in the viscometer probably lies in the order of 1000 poises. The range may be extended somewhat through the use of a more rugged torsion assembly than is currently in use, but the difficulty of embedding a slender ring in a trough-shaped cup eventually should prove too great for convenient operation.

By way of contrast, the lower limit of viscosity has not been reached, although the use of more slender filaments in the determination of stresses appears to be necessary. Development along these lines is currently in progress.

SUMMARY

A rotational viscometer has been designed and constructed for the determination of the viscosities of fluid dispersions at known rates of shear. Features of the viscometer which have been instrumental in providing high sensitivity at low shear rates are:

1. A completely frictionless suspension.
2. Application of shearing stress to both sides of the torsion member.
3. Absence of end effects, or at least their relegation to second order effects.
4. Large torque arm coincident with the requirement of only a few milliliters of sample.
5. Heat transfer to and from the thermostatic medium through both walls of the cup.
6. A torque measuring assembly of low inertia.
7. Minimization of effect of eccentricity in the cup-ring assembly by the use of a null point method for determining deflections.
8. A closer approximation to the ideal parallel plate viscometer than is customary with rotational viscometers.

A critical evaluation of the performance of the instrument has been made, and an insight has been given into the types of problems which can be studied by means of the viscometer. Results of studies on printing inks, pharmaceuticals, and other dispersions will be presented in the near future.

ACKNOWLEDGMENTS

The authors are indebted to the Squibb Institute for Medical Research for support of this work, for the encouragement given by Elliot Bartner, and for the supervision of the construction by Edward Grubel of that organization. Lehigh University personnel who participated in the evaluation of the viscometer were J. C. Miller, G. J. C. Frohnsdorff, M. N. Hoechstetter and R. C. Budias, whose contributions are gratefully acknowledged.

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Effect of Fillers . . .

(Continued from page 23)

however, large differences are indicated between different fillers.

No attempt will be made to quantitatively explain the damping results. It is interesting to note, however, that it is possible for the heat produced (proportional to $G\delta$) to be greater in a filled material than in the unfilled polystyrene while at the same time the damping δ is less for the filled than for the unfilled material. Zenzer (2) has shown that slippage between grain boundaries in metals can give rise to energy dissipation. The excess of energy dissipation in the filled materials over that of unfilled polystyrene might be due to a slippage of the filler particles in the plastic matrix.

SUMMARY

The dynamic shear modulus and mechanical damping were measured on polystyrene containing various

(Please turn to page 61)

Time and Temperature Effects on the Deformation of Asphaltic Bitumens and Bitumen-Mineral Mixtures

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ABSTRACT

The development of a nomograph which allows easy determination of the stiffness of bitumens as a function of time and temperature is briefly described. This system requires the knowledge of two routine test data of the bitumen, one to indicate its hardness (ring and ball softening temperature), the other to indicate its rheological type (penetration index).

A further study has revealed that for all deformations the deformation (stiffness) of bitumen-aggregate mixtures of low-voids content is a function of only the stiffness of the bitumen it contains (this stiffness being taken at the appropriate time and temperature) and the volume concentration of the aggregate. As a consequence, a simple system can be built up by means of which the deformation of all bitumens and bitumen-mineral mixtures can be quickly derived with an accuracy sufficient for engineering purposes.

INTRODUCTION

Bitumen, either alone or mixed with mineral aggregate, is used as a roadmaking or a building material, and the engineer consequently is interested in the deformations and stresses occurring under the prevailing conditions. Being a visco-elastic material, however, its mechanical behavior depends on time of loading (frequency) and temperature. Moreover, the great number of bitumen grades on the market differ considerably in properties, and in most applications the bitumen is mixed with large quantities of mineral aggregate. Since all these factors influence the mechanical behavior, describing or predicting the properties of a mixture from a small number of basic data seems an impossible task.

We have found, however, that the behavior of bitumens alone can be described in a simple way by using a nomographic representation of experimental results. All essential factors can be incorporated in this nomograph and a good correlation with data from standard test methods obtained. Only the essential principles of this nomographic representation of the behavior of unmixed bitumens will be discussed in this paper; for details the reader is referred to two earlier publications.[1,2]

Following these results it will be shown how the behavior of a bitumen aggregate mixture is related to the properties of the particular bitumen it contains and the amount of aggregate added.

DEFINITION OF STIFFNESS

For simplicity we shall consider only stresses and deformations so small that the two are proportional. This requirement is not a serious restriction, since the object in applications is generally to avoid excessive deformations and hence the nonlinear range is seldom of interest. In fact, at low temperatures and short times of loading, linear behavior is found to occur up to the point of brittle fracture. At high temperatures and long loading times most bitumens behave almost as a Newtonian fluid, and our condition is again fulfilled. There is a range of conditions, however, where some grades show distinct nonlinear effects; we shall refer to this point later.

Two ways of testing the material were used: the static creep test (constant stress experiment) and the dynamic test under a sinusoidally alternating load.

If linear behavior is postulated we may define a static $S(s)$ and a dynamic stiffness modulus $S(d)$ as follows:

$$S(s) = \sigma/\varepsilon = \text{Constant tensile stress/total strain} \quad (1)$$

$$S(d) = \sigma/\bar{\varepsilon} = \text{amplitude of alternating stress/amplitude of strain} \quad (2)$$

In general $S(s)$ is a function of temperature and time of loading; likewise, $S(d)$ depends on temperature and frequency.

If we calculate $S(s)$ for a Maxwell body we find:

$$S(s) = E_i / (1 + t/\theta_i) \quad (3)$$

where E_i is the Young's modulus of the Hookean spring and

$$\theta_i = \lambda_i / E_i \quad (4)$$

is the relaxation time; λ = Trouton's coefficient of viscous traction = 3 × normal viscosity coefficient in shear for an incompressible material, and t is the time the constant stress is operative.

For the dynamic stiffness we find

$$S(d) = E_i (1 + 1/\omega^2 \theta_i^2)^{1/2} \quad (5)$$

where ω is the angular frequency.

It is easily seen that both for t and $1/\omega << \theta_i$ and for t and $1/\omega >> \theta_i$ equations (3) and (5) yield the same results if $1/\omega$ is substituted for t or vice versa. Indeed Figure 1 shows that there is only a small range where $S(s)$ and $S(d)$ do differ appreciably if the same scales are used for plotting t and $1/\omega$.

If the same procedure is used for a model consisting of a Voigt element and a Hookean spring in series, results are as depicted in Figure 2. Here too there is not a great difference between the two curves from a practical point of view. In fact, the maximum dis-

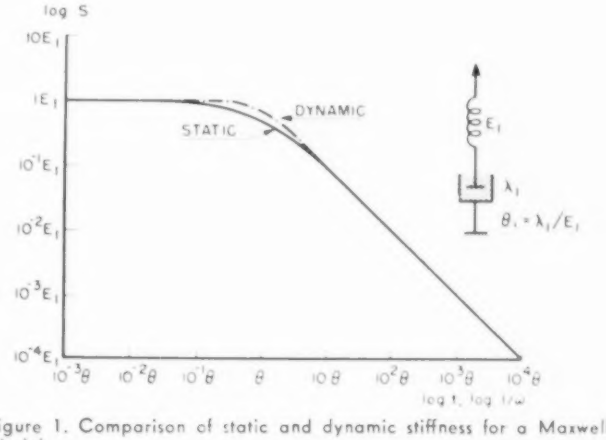


Figure 1. Comparison of static and dynamic stiffness for a Maxwell Model.

crepancy in both figures amounts to 40 percent. This may be a large difference when we are interested in relaxation spectra and correlation to molecular structure. From a practical point of view, however, the discrepancy is small compared to the enormous variation of many decades in the logarithm of the stiffness with temperature and frequency. No further distinction will be made, therefore, between S *(s)* and S *(d)* except as expressly stated.

As the behavior of any visco-elastic material which shows linearity can be represented by a model of an infinite number of Voigt elements in series,[3] S*(s)* and S*(d)*

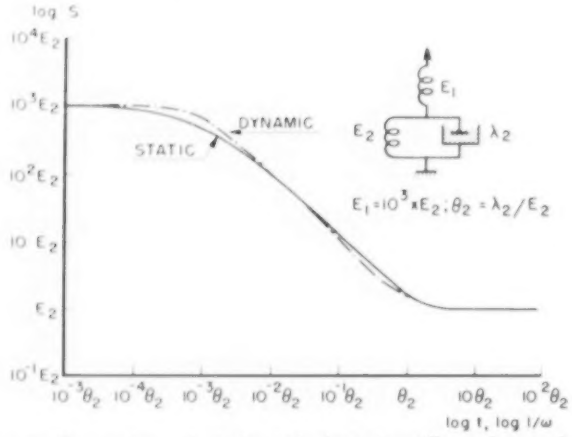


Fig. 2—Comparison of static and dynamic stiffness for a three-element model.

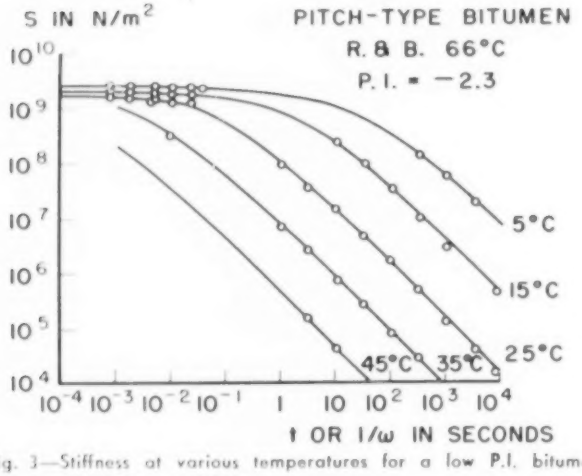


Fig. 3—Stiffness at various temperatures for a low P.I. bitumen.

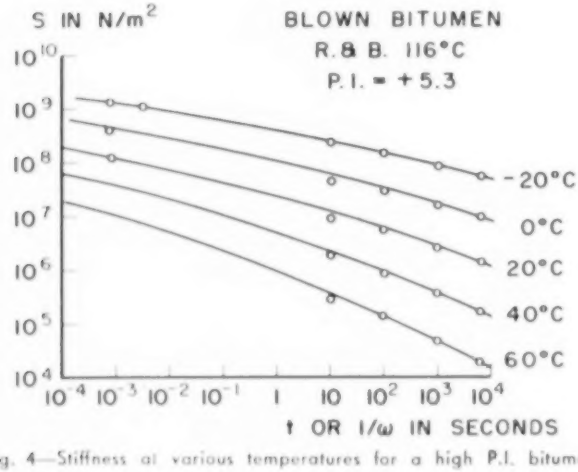


Fig. 4—Stiffness at various temperatures for a high P.I. bitumen.

can generally be considered identical for practical purposes.

From the foregoing, it is clear that for a perfectly elastic behavior, the stiffness S is independent of time or frequency and for a purely viscous behavior S is inversely proportional to time of loading or proportional to the frequency. In the latter case it follows from (3) and (4) that

$$S = 3\eta/t \qquad (6)$$

or

$$S = 3\eta\omega \qquad (7)$$

where η is the viscosity coefficient.

The two methods of loading cover a large field of practical applications. When a different loading procedure is used, the deformation can always be calculated from the results of either method. For, as we postulated a linear behavior, the superposition principle holds, and the deformation resulting from the stress actually applied can always be found by analyzing the stress-time curve, replacing it by a number of successive loading steps of known type, and adding the resultant deformations.

In fact, this procedure, when related to the dynamic stiffness, is identical to the well-known Fourier analysis.

EXPERIMENTAL RESULTS

Measurements were carried out in various ways. For the constant stress experiment, tensile and bending tests and rotation viscometers (Couette type) were used; for the dynamic method, flexural vibrations of free and clamped bars of the material.

As indicated, results of static and dynamic measurements are plotted in the same graph, the time of loading for the dynamic experiment being replaced by the inverse of the angular frequency ω.

There will never be any doubt as to whether the plot refers to a static or a dynamic experiment, since dynamic tests were used only for smaller times than 1 second and static tests only for longer times.

Typical results are shown in Figures 3 and 4 for two bitumens, one having a very low penetration index, the other, a blown bitumen, a rather high penetration index. Results are given for various temperatures.

Attention should be paid to the wide ranges of loading time and stiffness covered by these graphs.*

It is seen from Figure 3 that the low P.I. bitumen behaves almost purely elastically at short times of loading, to show a fairly sharp change at longer times, where the behavior rather soon becomes purely viscous (slope of 45°). The behavior of the blown bitumen is quite different, the decrease in stiffness with increasing time of loading being far smaller. This is due to retarded elastic effects being pronounced for this bitumen.[1]

The curves in Figures 1 and 2 are drawn in such a way that they coincide when shifted in the direction of the long-time axis. It is seen that in this way good agreement with the experiments can actually be obtained. This is a most important conclusion, for it greatly facilitates the description of the rheological behavior. Mathematicaly it can be expressed by saying that the de-

*In the system used here, the unit of force is the newton (N), the unit of mass is the kg and units of length and time are metre (m) and second respectively. These units differ from cgs units only by whole powers of 10. Consequently the unit for the stiffness is 1 N/m^2 = 10 *dyne/cm²* = 1,02 × 10^{-5} *kg/cm²* = 1,45 × 10^{-4} *lb./sq. inch*, while for the viscosity, 1 *N sec/m²* = 10 *dyne sec/cm²* = 10 *poise*.

pendence of the stiffness on time and temperature can for each bitumen be described by one variable, which is composed of the log of time plus some function of the temperature or:

$$f(S) = -\log t/t_0 + \chi(T) \qquad (8)$$

Here t_0 is some constant with a time dimension, and f and χ are empirical functions; t_0, f, and χ will differ for the two bitumens.

ROUTINE TEST METHODS REFERRED TO

Such a representation would not be of much help if it were necessary to determine the constant t_0, f, and χ for each bitumen separately. However, a fair correlation with data from routine test methods proved to be possible. These routine methods are briefly mentioned here; for further details the reader is referred to the literature.[4]

(a) Penetration. This is defined as the depth, measured in 1/10 mm, which a standardized needle penetrates into a bituminous surface under a constant load (100 g) in a specified time (5 sec). The method has been standardized by ASTM under No. D 5-25 and by IP under No. 49/46.

(b) Softening Point Ring and Ball. A ring of given dimensions, filled with bitumen, is loaded with a steel ball (3.5 g). The whole is heated in a bath, and the temperature at which the bitumen reaches a certain deformation is reported as the Ring and Ball softening point. The method has been standardized by ASTM under No. D 36-26.

(c) The Penetration Index (P.I.) This is not a test method but an index figure, introduced by Pfeiffer[4] and Van Doormaal to indicate the temperature susceptibility of the penetration of a bitumen. This requires a somewhat lengthier discussion.

If the logarithm of the penetration is plotted against the temperature (T), an almost straight line is obtained, suggesting the use of the slope of this line as a susceptibility index. This would require two penetration tests at two temperatures for each bitumen. If, however, the straight line is extrapolated to the temperature of the Ring and Ball softening point, the corresponding penetration is approximately 800. Therefore the P.I. can be calculated from one penetration and the Ring and Ball softening point, the slope of the penetration line being given by

$$\frac{\log 800 - \log \text{pen.}}{T\,(R\,\&\,B) - T}$$

where T refers to the temperature in °C at which the penetration test is carried out.

The penetration index (P.I.) as used by Pfeiffer and Van Doormaal is not directly proportional to this slope, but the relation is given by the formula:

$$\frac{\log 800 - \log \text{pen.}}{T(R\,\&\,B) - T} = \frac{20 - \text{P.I.}}{10 + \text{P.I.}} \cdot \frac{1}{50} \qquad (9)$$

This relation, which is quite arbitrary, was chosen for historical arguments, one of the reasons being that a Mexican bitumen with a penetration of 200 at 25°C gives a P.I. of zero.

As is clear from (9), a bitumen with a penetration which is completely independent of the temperature has a P.I. = +20, whereas the other extreme, a bitumen with an infinite temperature susceptibility, should have a P.I. = −10.

These extremes are never found in practice; the P.I. of bitumens varies from −2.6 to +8. The lower the P.I., the higher the temperature susceptibility.

The penetration index can easily be read from the nomograph given in Figure 5. As can be seen from that nomograph and also from equation (9), the only data required to determine the P.I. are a penetration value and the difference between the temperature at which it is determined and the temperature of the Ring and Ball test.

A close relationship exists between the P.I. and the rheological type of asphaltic bitumen. In fact this is the real importance of this index figure, as will be shown below.

NOMOGRAPHIC REPRESENTATION OF RESULTS

The experimental results of Figures 3 and 4 can be represented nomographically, as is clear from equation (8), by a line coordinate chart. Such a chart consists of three parallel lines on each of which are suitable scales for t, T, and S, the scale for t being a logarithmic one. This would not solve our problems, however, since for each different bitumen grade a different nomograph should be used.

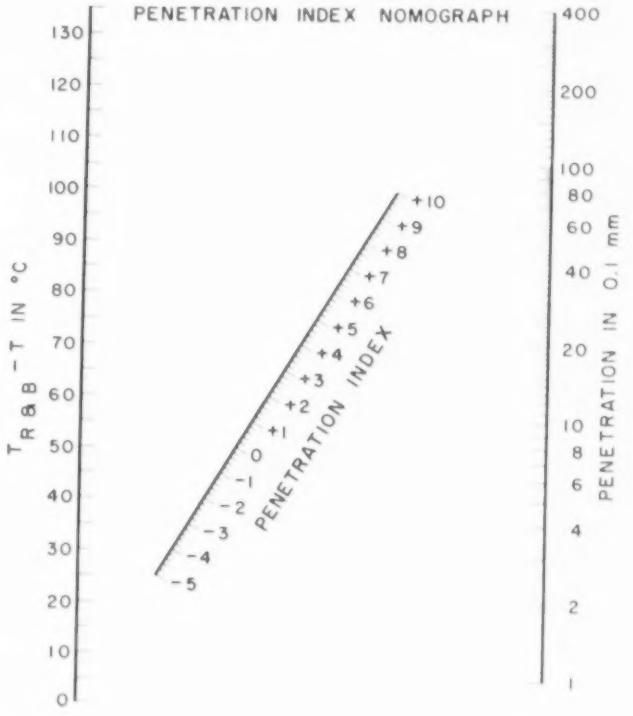
However, it was found that two bitumens of the same rheological type (same P.I.) at the same time of loading or frequency have equal stiffness at temperatures which differ from their respective Ring and Ball temperatures by the same amount. This means that equation (8) can be transformed to

$$f(S) = -\log t/t_0 + c\psi\,(T\,(R\,\&\,B) - T) \qquad (10)$$

in which the temperature function χ of T alone has been replaced by a function (ψ) of $T(R\,\&\,B) - T$.

A nomographic representation based on equation (10) in which the temperature scale now refers to the difference between $T(R\,\&\,B)$ and the temperature at which we want to know the stiffness has a far greater

Fig. 5—Nomograph for determination of Penetration Index.



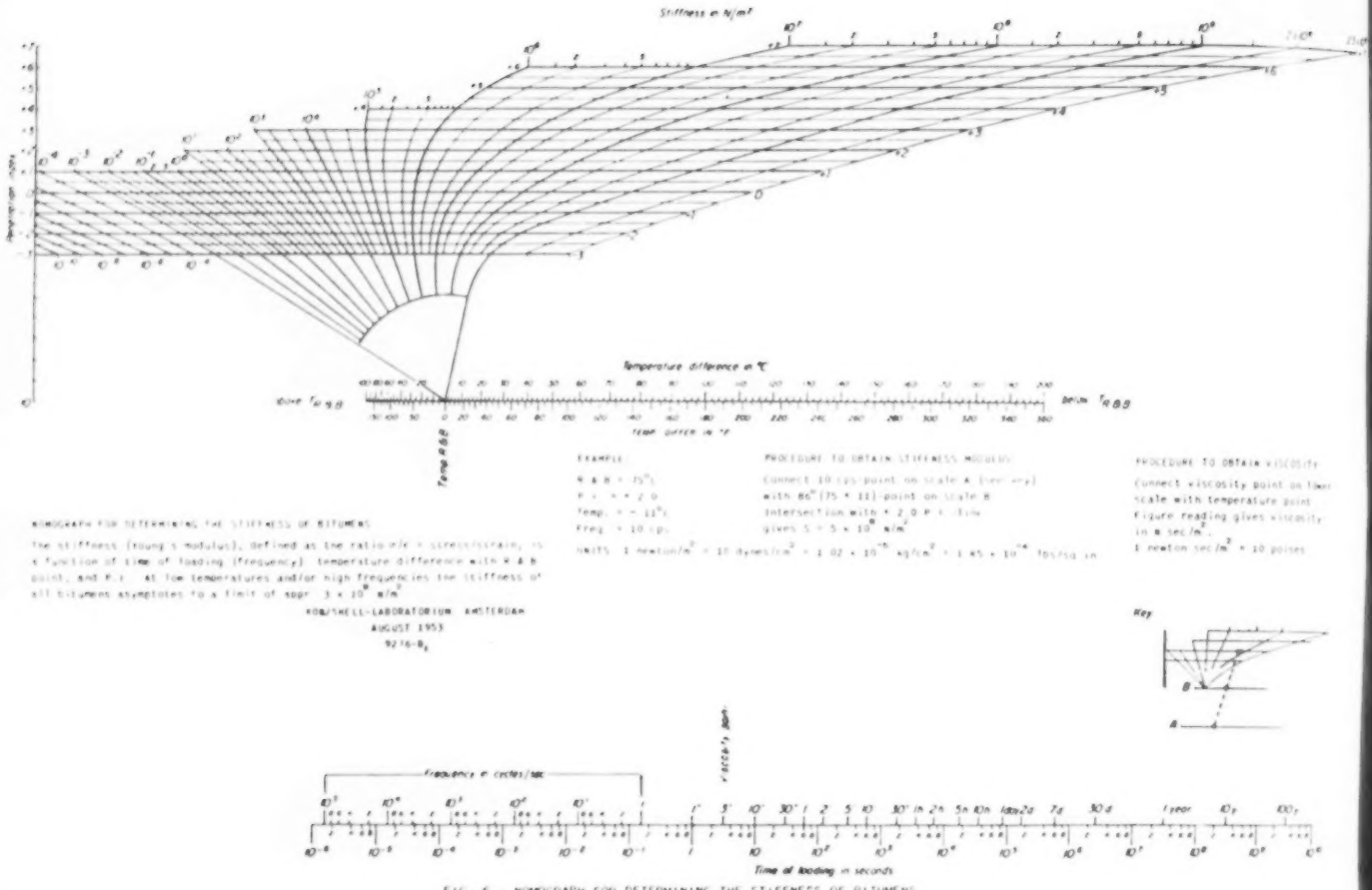


FIG. 6 – NOMOGRAPH FOR DETERMINING THE STIFFNESS OF BITUMENS

applicability, because it can be used for all bitumen grades having the same P.I. (same rheological type).

Moreover, it has been found that the same function ψ can be used for all types by giving the "constant" **c** a suitable value. In other words, by making c a function of P.I. only, equation (10) can be made to represent the stiffness of all bitumens as a function of **t**, **T***(test)*, **T** *(R & B)*, and P.I.; the f(S) also depends on P.I. This means that for all bitumens the same scales for time and temperature difference with Ring and Ball temperature can be used; the scale for S, however, lies at different distances and is graduated in different ways according to the rheological type (Penetration Index) of the bitumen.

On this basis the nomograph of Figure 6 was constructed, from which the S value can very easily be read. The use of the nomograph may be clear from the outline given in the figure.

In the following the nomograph will be used to deduce the stiffness of the bitumen in the range of $S > 10^7$ N/m² only. Figures 7 and 8 illustrate how nomograph readings in this range compare with experimental results.

In Figure 7 the stiffness at a frequency of 200 cps of a number of bitumens is plotted as a function of **T***(R & B)* — **T***(test)*; the curves in this figure are obtained from the nomograph of Figure 6. The dots and crosses on the P.I. = 2.1 line represent two different bitumens of the same penetration index which differ in R & B temperature by 53°C. The various symbols for P.I. = −2.6 indicate three different bitumens, the extremes of which differ by 20°C in R & B temperature. It is seen that points come close together and that there is a fair agreement with nomograph readings. Results of static tests for a loading time of 1000 sec are compared with nomograph readings in Figure 8. Again there is good agreement and also here the **T***(R & B)* — **T***(test)* can be shown to hold: there are experimental results of three bitumens for P.I. = −0.4, the extremes of which differ 15°C in R & B hardness.

In general, there is also good agreement between the nomographic representation and experimental results in the lower range of the stiffness. For details we refer to an earlier publication.[2]

As can readily be seen in Figure 7, at low temperatures the stiffness of all bitumens asymptotically approaches a limit of 3×10^9 N/m² (3×10^{10} dyne/cm²). At these temperatures the stiffness also becomes independent of the frequency (Figure 3), and the material

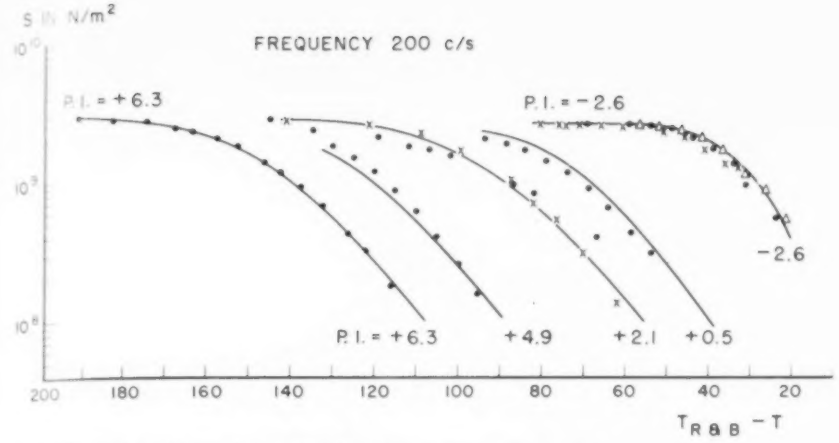


Fig. 7—Dynamic stiffness from vibrating bar experiments for various bitumens plotted against the temperature difference between R&B and test temperature.

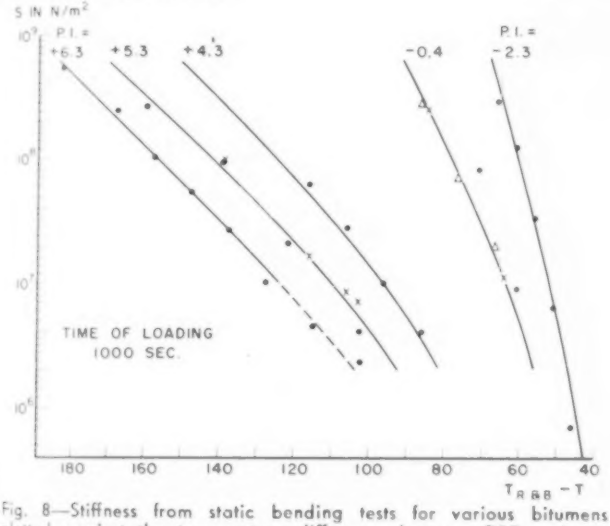


Fig. 8—Stiffness from static bending tests for various bitumens plotted against the temperature difference between R&B and test temperature.

behaves as a purely elastic one. It is also apparent from Figure 3 that the behavior of a low P.I. bitumen may change from purely elastic into purely viscous very rapidly when the frequency is decreased by a factor 10^3 or the temperature is decreased 10° to 20°C. In fact, this bitumen behaves almost like a Maxwell body, as may be seen by comparing the curves of Figure 3 with those of Figure 1.

The upper left part of the nomograph is blank and does not permit reading of the stiffness. In this range the higher P.I. bitumens show nonlinear effects, and description with a stiffness modulus becomes inadequate. Extrapolation into this region should not be made.

BEHAVIOR OF MIXTURES OF BITUMEN AND MINERAL AGGREGATE

As long as stresses and deformations are small, we can again introduce the stiffness to describe the properties of a mixture. In the results given below, which are all obtained by dynamic testing methods, the condition of linearity is fulfilled throughout.

Two experimental methods were used: flexural vibrations of a bar, and torsional vibrations of a cylinder of the material. Bars for the bending vibration test are 1 cm thick, 2 cm wide, and 10 to 25 cm long. Bars are suspended in the nodes of the vibration type chosen and set into vibration electromagnetically by attaching a small plaque of iron on one end. Opposite this iron plaque is a small electromagnet which is energized by a resistance-capacitor generator giving a current of adjustable and accurately known frequency. On the other end of the bar a similar plaque is attached opposite which is an electromagnet activated by the vibrating bar. The current in the latter electromagnet is used to register the amplitude of the vibrations. The bar is brought into resonance and from the resonance frequency, dimensions, and density, the stiffness can be calculated. By having each bar vibrating both in the basic frequency and in a few overtones and by using bars of various length, the stiffness can be determined at a number of frequencies between 50 and 2000 cps. This method was used for bitumens alone (results given by Figure 7 were obtained in this way) and for mixtures containing not too coarse mineral matter. Moreover it is suitable in the high stiffness region only, since in the lower region damping is usually too high to permit accurate determination.

For the torsional vibration test, cylindrical rods 4 to 8 cm in diameter and 10 to 30 cm long are used. Samples of this size can be manufactured in molds under high pressure, simulating conditions of compaction used in road-building. Also, the dimensions permit the use of coarse particles (up to 1 cm). The cylindrical sample is fixed tight at its lower end to an infinite mass (the laboratory building), and to its top a cylindrical mass many times larger than that of the sample is attached. Torsional vibrations are set up in this system around its axis by means of eccentric weights attached to the shaft of an electric motor which is fixed to the upper mass. From the resonance frequency of the system the stiffness in shear

$$S(G) = \frac{\text{amplitude of shear stress}}{\text{amplitude of shear strain}} = \tau/\gamma$$

can be calculated. By using masses of varying moment of inertia the stiffness may be obtained at various frequencies ranging from 10 to 150 cps. Owing to the mechanical way of excitation this method can be used in a lower region of stiffness, where damping is usually higher and more energy is required to sustain vibration.

Experimental results obtained by adding various amounts of mineral matter to the same bitumen are shown in Figure 9 as a function of temperature. It is seen that the stiffness of the mixture increases regularly with increasing concentration of the mineral $[\mathbf{C}(c)]$ and this effect is slightly greater at higher temperatures, where the bitumen is softer.

Figure 10 is an example of results obtained when measurements are carried out with bitumens, but at temperatures such that the bitumen component shows the same stiffness. For the two bitumens in this figure the stiffness of the bitumen alone is 2×10^9 N/m², which is only slightly less than the maximum value. The stiffness of the mixture has been plotted as a function of the volume concentration of the mineral aggregate $[\mathbf{C}(c)]$.

In spite of the fact that a great variation in particle size and grading of the minerals (fillers, sand, and mixtures of the two) has been used, all points lie practically on one curve. Evidently, these factors do not affect the

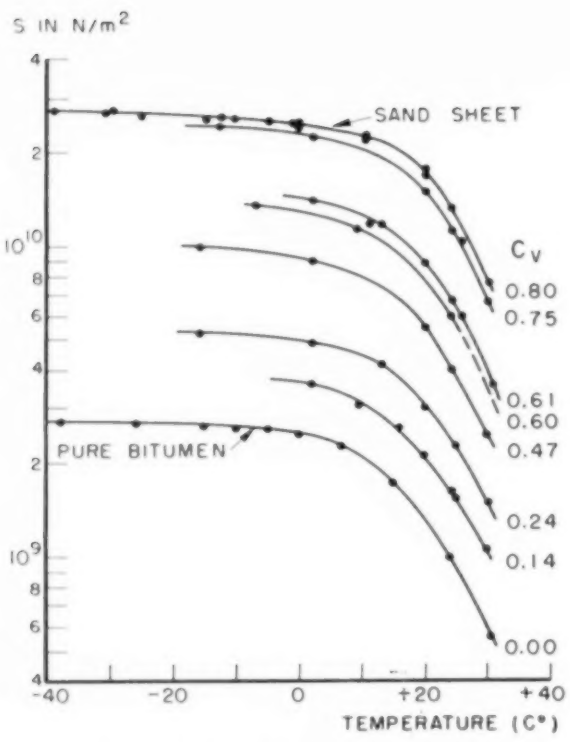


Fig. 9—Dynamic stiffness of mixtures containing the same bitumen and various amounts of aggregate (sand and/or filler) as a function of temperature. Properties of the bitumen: R&B 54°C, pen. 13, P.I. -2.6.

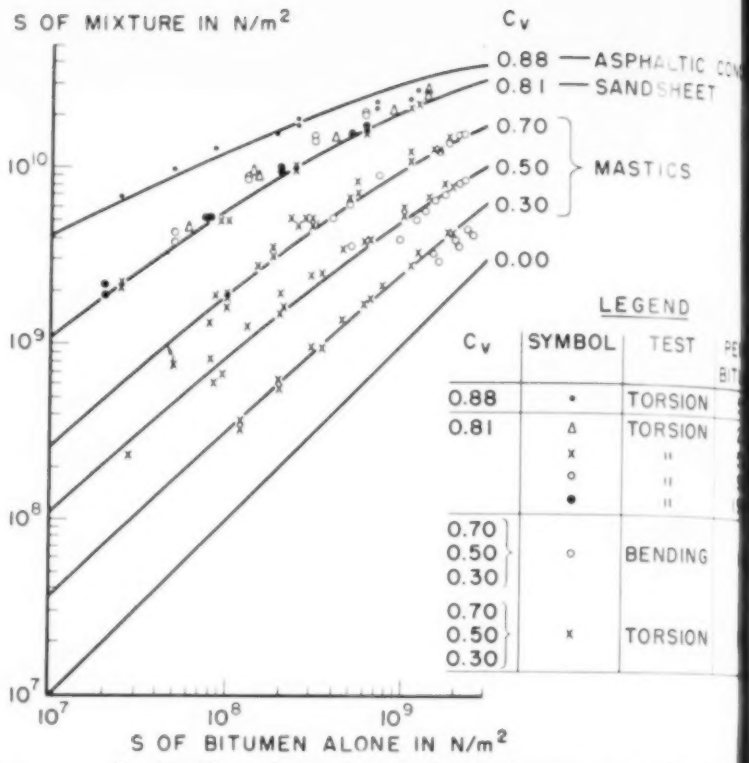


Fig. 11—Effect of concentration of mineral aggregate on stiffness of mixtures. Symbols either refer to different bitumen grades or to different experimental methods.

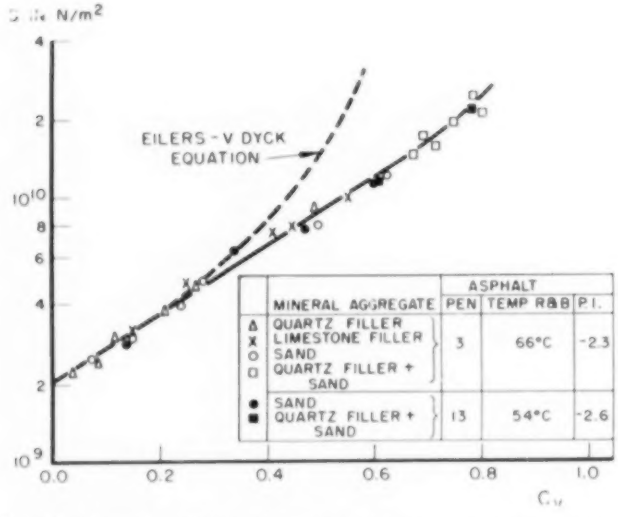

EILERS - V DYCK EQUATION
ASPHALT
MINERAL AGGREGATE | PEN | TEMP R&B | P.I.
Δ x o □ QUARTZ FILLER, LIMESTONE FILLER, SAND, QUARTZ FILLER + SAND | 3 | 66°C | -2.3
• ■ SAND, QUARTZ FILLER + SAND | 13 | 54°C | -2.6
Cv

Fig. 10—Dynamic stiffness of various mixtures as a function of the volume concentration of mineral aggregate (Cv). Temperature and frequency conditions are such that the pure bitumen contained in the mixtures has a stiffness of 2×10^9 N/m².

results, so that it is only the volume concentration of the mineral that counts. Similar curves can be drawn when other values are chosen for the stiffness of the bitumen.

Results shown in these last two figures led to the hypothesis that the stiffness of a mixture is governed by two factors only: (a) the volume concentration of the mineral aggregate [$C(v)$] and (b) the stiffness of the bitumen alone.

To check this hypothesis further, additional experiments were carried out in the lower stiffness region with a variety of mixtures. Contrary to the results shown in Figures 9 and 10 most of these experiments were carried out with the torsional vibration test. Apart from a number of mastics (up to 70 vol percent mineral) sandsheet and asphaltic concrete mixtures were also measured; they differ from the mastics in that they cannot be poured at high temperatures, but have to be compacted in molds. This compacting requires some time, during which the mixture must be kept at a high temperature, resulting in a hardening of the bitumen owing to oxidization. To allow for this hardening the samples after being tested were put in carbon disulphide in which the bitumen readily dissolves. The bitumen was then carefully recovered from the solution and its R & B and P.I. determined. The R & B generally showed a rise of a few degrees centigrade as compared to the original bitumen, the P.I. increased only by a few tenths depending on the treatment of the sample. No difference in properties was found between original bitumen and bitumen recovered from mastics.

With the help of these data the stiffness of the bitumen as contained in the mixture can easily be read from the nomograph of Figure 6. When now we plot the stiffness of the mixture as measured against that of the recovered bitumen as determined from the nomograph at the appropriate temperature and frequency, a single curve for all types of bitumen results if the same volume concentration of mineral is used.

An illustration of this general result is shown in Figure 11. In order to enable an easy comparison between results obtained with the two experimental methods the stiffness in shear obtained from the torsional vibration test is multiplied by a factor three to obtain the (normal) stiffness in tension. This is allowed when the material can be considered as incompressible, which is true in the lower stiffness region. If the stiffness of the bitumen approaches the upper limit of 3×10^9 N m closely, neither the bitumen alone nor the mixture is incompressible. Then Poisson's constant may be high

as 3 for the pure bitumen[1] and 4 for the mixture, which requires multiplication by a factor 2.7 to 2.5 in that region. In general, however, the correlation at which we aim is not that accurate, and for the sake of simplicity a factor of three is used throughout.

If we now look at Figure 11 more closely, we first see that there is a reasonable agreement between the results obtained with the two experimental methods used for measuring the mastics. Furthermore it is seen that there is no systematic difference between results obtained with the various bitumen grades used in the sandsheet mixtures though they do differ significantly in hardness. Also there is a systematic increase of the stiffness with increasing volume concentration of mineral, as was shown in more detail in the foregoing figure.

Many more results obtained by the author confirm the general trend shown in these few examples. A number of other types of bitumens were used, most of them being of the conventional distilled type like those for which results are shown in Figure 11; but three blown grades were also employed with penetration indexes up to +6.0, one of them being a blended grade. Results from all these tests agreed closely with the results shown in the figure given here.

As to the minerals used in the mixtures, all contained both sand and fillers except the asphaltic concrete mixture which contained 50 parts by weight of chippings to 100 parts of mineral. The amount of fillers used range from 30 p.b.w. [$\mathbf{C}(v) = 30\%$] to 7 p.b.w. [$\mathbf{C}(v) = 88\%$].

The fillers used were limestone or quartz, both having particle sizes largely between 2 and 70 microns, the amount of particles below 2 microns being 7.7 percent and 7.0 percent of total filler for limestone and quartz, respectively.

The quartz filler was rather coarse and contained 28 percent particles larger than 50 microns, the limestone filler only 2 percent. Clays, carbon black, and other fine particles were not used.

All these results show that the stiffness of a bitumen-mineral mixture is determined only by the stiffness of the bitumen alone and the volume concentration of the mineral aggregate.

This statement applies to the conditions investigated, i.e., to linear mixtures with bitumens having a voids content below 4 percent.

The influence of the type of bitumen, the temperature, and the frequency are all accounted for by the stiffness of the binder.

It is clear that this is an important practical result, for with the help of Figure 11 and the nomograph of Figure 6 it is now possible to deduce immediately the stiffness of a certain mixture under any condition if only three material data are given: R & B and P.I. of the bitumen, and $\mathbf{C}(v)$ of the aggregate in the mixture.

The curves in Figure 11 have a clear tendency to converge on the point where both S mixture and S bitumen are 7×10^{10} N/m^2. This value therefore represents the stiffness (elastic modulus) of a solid mineral sample. It is clear that if the bitumen could have the same stiffness, the stiffness of the mixture should be independent of $\mathbf{C}(v)$.

Now the bitumen cannot have a stiffness beyond 3×10^9 N/m^2 but this is high enough to deduce the effect of adding large amounts of aggregate, as compared to the effect when the bitumen is softer. For example the asphaltic concrete mixture has a stiffness which is about 400 times that of the bitumen at 10^7 N/m^2 and only 50 times at 10^9 N/m^2.

It may now also be clear that formulas found in literature for the effect of adding mineral matter to viscous fluids do not apply for our conditions, since it is always assumed that the dispersed material is infinitely hard as compared to the fluid. Moreover most formulas are only valid for very small concentrations.

For concentrations up to 30 percent, however, it was found that our results can be represented quite well by an empirical equation of Eilers[5] and van Dijck reading:

$$S_{mix} = S_{bit} \left[1 + \frac{1.25\,\mathbf{C}(v)}{1 - 1.28\,\mathbf{C}(v)}\right]^2$$

This equation was made to obey Einstein's[6] well-known formula for very small concentrations and to give an infinite value for the ratio when $\mathbf{C}(v) = 0.74$, which is the volume concentration of the densest packing of spheres of equal diameter.

How this formula applies to our data for a high bitumen stiffness is shown in Figure 10. It is seen that up to $\mathbf{C}(v) = 0.30$ there is a very good agreement. Also from Figure 11 it is seen that the line for $\mathbf{C}(v) = 0.30$ is straight and has a slope of practically 45°, which means that the formula also holds for lower values of the stiffness.

If however the concentration is larger than 30 percent, the effect of adding mineral cannot be accounted for by simply multiplying the stiffness of the bitumen by a factor depending on $\mathbf{C}(v)$ only, but the results also depend on the stiffness of the bitumen as indicated above.

ACKNOWLEDGMENTS

The author is indebted to Dr. R. N. J. Saal and Messrs. F. Dijkstra and W. Heukelom for their interest and assistance and to the management of the Royal Dutch Shell Group for their permission to publish this work.

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Viscous Heat Effects . . .

(Continued from page 40)

β_1 = constant in parabolic law temperature dependence of viscosity § = 2.05 x 10^{-2}

β_2 = constant in parabolic law temperature dependence of viscosity § = 1.14 x 10^{-4}

*Estimated

**Rough estimate

†Calculated from the $D_R(\tau)$ line on figure 4

§Calculated from Tordella's flow curves at 150°, 190°, and 250°C.

In Table 4 are given the results of the calculations based on the above assumed values of the physical properties. These results include the maximum temperature attained in the extrusion process, the average exit tem-

Viscous Heat Effects . . .

(Continued from page 53)

perature of the fluid, and the deviation in the flow curves resulting from viscous heat effects. The experimental setup would be expected to give results lying somewhere between the isothermal wall and the insulated wall results, but probably closer to the latter.

It is unfortunate that there are no data available on the average exit temperature to compare with these calculations. Above $\tau_R = 5 \times 10^6$ the theory predicts temperature effects sufficiently large that some physical properties of the fluid might be affected. A comparison is given between the theoretical and experimental D/D$_o$ values; it is seen that the calculated values are generally somewhat too small. It is difficult to draw any conclusions concerning the discrepancy, in view of the doubtful values of the physical constants used in the calculations. Nevertheless the theory does seem to predict the value of τ_R above which viscous heat effects become important. Furthermore the general shape of the predicted flow curve is correct.

TABLE 4

Tabulation of Calculated Results for the Viscous Heat Effects in the Extrusion of an "Alathon" 1 Polyethylene Resin.

			Isothermal Wall			Insulated Wall			
τ_R (dyn cm^{-2})	$\zeta \frac{\tau_R}{7 \times 10^6}$	τ_R	Tav (°C)	Tmax (°C)	D/D$_o$	Tav (°C)	Tmax (°C)	D/D$_o$	D/D$_o$ Expt'l[15]
$2 \cdot 10^6$	1.75	0	0	1.0	0	0	1.0	1.0
$5 \cdot 10^6$	0.28	0	0	1.0	1	2	1.0	1.0
10^6	0.070	1	2	1.0	4	7	1.1	1.2
$2 \cdot 10^6$	0.0175	4	7	1.1	12	24	1.4	1.8
$5 \cdot 10^6$	0.0028	20	52	1.6	37	130	2.6	4.3
10^7	0.0007	69	240	3.9	94	490	6.7	10.4

ACKNOWLEDGMENTS

The author wishes to thank the staff of the Polychemicals Department of the Du Pont Experimental Station for suggesting this problem and for making their facilities available to him. Particular thanks are due Drs. W. H. Linton and J. P. Tordella with whom the author had many interesting and profitable discussions. During the latter part of the work on this problem, financial support has been provided by the National Science Foundation. In addition, the author desires to thank Prof. Charles F. Curtiss (Chemistry Department, University of Wisconsin) who read the manuscript and suggested some changes, and Mr. T. H. Chin (Electrical Engineering Department) who assisted with some of the calculations.

Dynamic Creep of Plastics . . .

(Continued from page 21)

ACKNOWLEDGMENTS

The results reported in this paper represent a part of a Master of Science Thesis by W. J. Schuman conducted under the supervision of Joseph Marin. The authors appreciate the technical help of Dr. L. W. Hu and the financial assistance of the Research Corporation in conducting this research.

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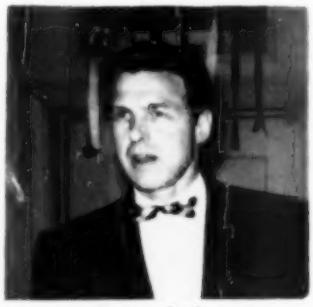
GOLDEN GATE

R. G. Newhall

The May meeting of the Golden Gate Chapter under the leadership of president **John L. Morrill** featured a lively discussion of the potential for large area vacuum forming and an elaboration of some of the unusual problems involved. Program chairman Roy Theiss introduced the speaker, George Parker of Dimensional Displays, Inc., who showed a series of slides illustrating some of the large vacuum formed signs produced by his company for its affiliate, Shopping Center Promotions. These signs, internally illuminated displays some 9' high, were mounted on lamp posts as part of an overall decorative scheme for a shopping center.



George Parker

These displays are vacuum formed from Eastman's Tenite butyrate extruded by Jet Specialties Company in sheets 0.080" thick, 54" wide and 96" long. Dimensional Displays uses a double table vacuum forming machine of its own design. The sheets are warmed by Chromalux strip heaters 66" long each having an input of 3,000 watts. These strip heaters, arranged in banks of 35, give an average power density of 10-12 watts/sq.in. The total power input is accurately controlled by a percentage timer. The present machine is somewhat limited by the size of its vacuum tank—80 gallons—and the speaker pointed out that tank size was generally more important than pump capacity in large area vacuum forming.

THE *Injection* MOLDERSCOPE

By A. R. Morse
President, Injection Molder's Supply Co.

"It is the freedom to disagree, to quarrel with authority, in intellectual matters to think otherwise, that has made this nation what it is today." Dr. James B. Conant

Technical Notes On Nylon Molding

SHUT-OFF NOZZLES

Shut-off type nozzles should NOT be used for general nylon molding work because they may cause dangerous blow backs thru the feed opening, if the material in the cylinder is accidentally overheated or heated for prolonged periods. Only in very rare cases where excessively high front cylinder temperatures must be used (as in molding some very thin wall sections) can shut-off type nozzles possibly be suggested. Even then the danger of blow backs would still recommend consideration of the standard Du Pont design.

With shut-off nozzles no way exists of checking the condition of the melt in the cylinder. The nozzle should be open between shots to permit observation of the material or quick purging when necessary, or to allow the cylinder to relieve itself if overheated without dangerous blow back.

Nozzles with spreaders or torpedoes have not proven satisfactory because the molding range is much too narrow. Nozzles with reverse tapers of less than 1" are generally not satisfactory.

THE DU PONT DESIGN

The Du Pont design nozzle features a 1¼" long reverse taper, and heated extension so the restricted throat of the nozzle is directly under a special extra high wattage heater band. The design is based on exhaustive tests of dozens of types of nozzles of every conceivable type by Du Pont technicians. Furthermore, IMS has built and tested over 10 different types proposed for manufacture by various persons or concerns, but none was without some vital objections for general use.

The final Du Pont design represents the simplest, cheapest, safest, best, most foolproof and universal nylon nozzle conceived to date. Its successful use under the widest variety of molding conditions, completely bears out its practicability. Any "novelty" nylon nozzle should be considered only after re-checking EVERY detail of your overall molding operation such as thermocouple condition and location for nylon; heater bands and their arrangement, condition, and wattage; condition of pressure gages; line voltages; pump condition; booster relays and timers and all process factors that could affect the molding operation. If the nylon is adequately dry and the temperature of the cylinder and nozzle is properly distributed and controlled, the standard reverse taper design can be used to mold practically any job with practically any heater.

Adaption To Different Cylinders

Due to the added 1¾" heated extension necessary to incorporate the 1¼" internal reverse taper, nylon nozzles are perforce longer than the standard nozzles. Two methods can be used to adapt them to standard equipment. First, an alteration can be made to the mold to recess the sprue bushing, provided the die can support the 2½" to 2¾" clearance hole required. Or second, a bolster plate can be inserted back of the stationary half of the mold to permit the extension nozzle to be used. Sagetaining and reducing the sprue is recommended as faster cycles are often possible with modern sprues that require less plastic to fill them.

Stock Nozzles

Certain machines have enough nozzle space on them to permit use of standard stock nylon nozzles. These are notably the Reed-Prentice with Reed or IMS Heating Cylinders; the Lewis; the Stokes; and the IMPCO. Lester, HPM, Watson and most other types require special length nozzles designed specifically to fit certain molds.

Drooling and Freezing

Excessive drooling or stringing of the nylon from the nozzle is an indication that something is wrong with the handling of the material prior to molding, or that improper heating and temperature control are being used. Material should be completely dry, and never exposed to atmospheric conditions for long periods prior to molding.

Relocation of thermocouples can often correct drooling, as can selective re-arrangement or re-grouping of the cylinder heater bands. Use of new heavier wattage bands in the rear zone, and reduction of wattage in front zone permit most any standard heater to be successfully adapted to nylon molding with the nylon design nozzle.

Incorrect or misjudged cylinder ratings in terms of nylon can also cause excessive drooling. A shut-off type nozzle will NOT correct for these conditions, and by permitting SOME molding to be done under improper conditions can result in the production of under-strength or dimensionally unstable nylon parts.

While in general nylon can be molded on any press, the cylinder should be re-rated in nylon, and a study of the heating zones made to assure that too much heat is not being pushed into the nylon in the front zone.

This is almost unavoidable with certain types of standard cylinders where the chief contact or support area of the spreader is at the very front of the cylinder; and re-arrangement of bands, couples and wattages is essential here to re-adjust the heat input to the slow melting properties of nylon. If impractical, or if drooling persists, move the job to a (preferably) larger or different make of press.

(This topic will be continued in the October issue.)

Cleveland

R. M. Hershey

Mr. Ernest P. Moslo, President of the Moslo Machinery Company, addressed the May meeting of the Cleveland-Akron Section on "Runnerless Injection Molding and Some Problems of the Injection Machine Builder." Approximately 50 members attended the meeting which was held May 9 at the Spanish Tavern in Brecksville, Ohio.

Mr. Moslo discussed problems incident to building the perfect injection molding machine and some of the reasons why they cannot get the dream machine out of stock. Mr. Moslo's talk was published in the April 1955 issue of the SPE Journal. The movie shorts, "Play Better Golf," "Fundamental and Advanced Golf," and "Saving Strokes with Snead" were shown following the meeting.

The annual golf outing was held on June 10 at Columbia Hills Country Club. A film of the Cleveland Browns Championship game was shown after dinner.

The first meeting of the fall will be held on September 12 at the Spanish Tavern in Brecksville, Ohio. The October 10 meeting will feature the semi-annual Guest Night and the annual presentation of the annual Thomas E. Orr award to students of Case Tech and Akron University.

SECTION NEWS

Edited by

Stanley Bindman

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Western New England

George Snyder

Frank Reinhart, national president, (SPE) made the wind-up meeting of the current business year a special event for the 85 members of the Western New England Section at Bradley Field's Terrace Dining Room on May 4.

Mr. Reinhart outlined what he called his "three-point program" for developing the necessary philosophical goals to make SPE a dominant force behind the growth of plastics technology. Using the initials of "S-P-E" as the inspiration for his program, he listed the three areas as (1) Service to industry and humanity through plastics engineering, (2) advancement of Profession through the elevation of the professional status and ideals of plastics engineers, and (3) Education of youth in plastics technology through recruitment and guidance programs at the college training level.

Lending added significance to Mr. Reinhart's message was George W. Martin's (section prexy) announcement of newly elected directors for the Western New England Section for the next term. The new directors are: Arthur W. Logozzo, (present program chairman) Nutmeg Chrome Corp., president; Lewis H. Gustafson (present treasurer) Pro-phy-lac-tic Brush Co., vice president; Edward J. Sweeney (present credentials chairman) Rogers Corp., treasurer; Raymond A. Mazur, American Cyanamid, re-elected secretary. Other section directors elected to head special committees were: Richard J. Plichta, Fuller Brush Co.; Alvino Aliberti, A. & M. Tool and Die Co., Inc.; and Paul Stahlberg, Monsanto Chemical Company.

The first technical speaker of the evening's program was Gordon Seiter, manager of the High Vacuum Metallizing Dept., F. J. Stokes Machine Co., Philadelphia. Mr. Seiter gave a highly informative talk on vacuum metallizing plastic parts. He confined his discussion primarily to the advantages of vacuum metallizing as a production process, and outlined some of the latest advances made in this field. One of the more enlightening sections of his presentation, which was profusely illustrated with visual aid slides, pertained to the vaporization of aluminum strips by means of electrically-activated tungsten elements in the vacuum chamber. He also fully evaluated some of the latest types of vacuum equipment now being used, which generated a high degree of interest in the group, both during his talk and in the discussion period that followed.

Second speaker of the meeting was Dr. Harold Narcus of the Electrochemical Company, Worcester, Mass., who spoke on "Industrial Applications for Metallized Conductors." Dr. Narcus exhibited a number of metallized and electroplated industrial items, with special emphasis on the electronics field. Two of the industrial areas Dr. Narcus explored for the group were printed circuitry and the use of conductive paints for plating work.

Clyde E. Morrell, Pro-phy-lac-tic Brush Co., served as technical chairman for the meeting.

Frank Reinhart being greeted at the airport at Western New England.

Central Indiana

C. R. Smith

A total of fifty guests and members attended Central Indiana Section's May meeting at the Warren Hotel in Indianapolis. Speaker for the evening was Mr. A. R. Morse of Injection Molders Supply Co., Cleveland, Ohio.

Mr. Morse's interesting talk pointed up the lack of standardization within companies and the industry as a whole on replacement parts for injection molding equipment. While acknowledging the necessity of some variables, he illustrated with picture slides efforts of his company to reduce the molders required inventory of spare parts by making them interchangeable through the use of adaptors or multiple units.

At the conclusion of Mr. Morse's talk, Section President Gene Quear of Delco Remy Div. of G.M.C., Anderson, Indiana, outlined briefly to the group, progress being made by a joint committee of General Motors and S.P.I., in setting up certification standards for injection molding machinery. To date tentative approval has been obtained from major machine manufacturers for certification of the following:

1. Clamping capacity.
2. Method of rating single shot and plasticizing capacity.
3. Machine nozzle and locating hole.

Southern

C. Veazey LeCraw

The Southern Section met June 20th at Mammy's Shanty Restaurant in Atlanta. In behalf of the membership, President Diamond presented a plaque to Mr. Raymond W. Kruse and to Mr. Jules W. Lindau, III, first President and first National Council Representative respectively, in appreciation of the fine work they did in forming the Southern Section and in getting the organization rolling during its first year. The President summarized a report given to him by our National Council Representative on the success of the June 17th Council Meeting in Minneapolis, Minn.: 1. the Society proposes to publish engineering textbooks on plastics.

Mr. Diamond then introduced his guest and main speaker for the evening, Mr. Norm Huber of the Market Development Department of the Koppers Company. Mr. Huber gave an exceptionally fine talk on Koppers' expandable polystyrene. There were many examples of molded and extruded expandable styrene, plus many excellent slides to support the talk. After a long question and answer period, the members gave Mr. Huber a long round of applause for his fine talk.

New York

J. T. Dugan

The New York Section, SPE, attempted to answer the question of "Which Plastic Material to Choose—Thermoplastic or Thermoset?" at their May 18 panel meeting at the Gotham Hotel, New York, N. Y. Approximately 100 members and guests attended the meeting which saw T. Earl Dudney, Eastman Chemical Products, Inc., and E. Y. Wolford, Koppers Co., Inc., representing the thermoplastic materials; Edward F. Borro, Durez Plastics & Chemicals, and J. Moylan, Barrett Division, Allied Chemical & Dye Corp., as protagonists for thermosetting plastics. Guy A. Martinelli, Sylvan Plastics, Inc., served as moderator.

According to Mr. Borro, the decision must be reached in the first stages of engineering. The choice, too, must be made from among many materials within a category; for example, there are over 100 active phenolic materials with 18,600 different combinations possible. As a general rule, thermosetting materials are applicable where electrical properties, dielectric strength, dimensional stability are required. Corrosion resistance and low price are two other prime factors.

The second speaker, Mr. Dudney, cited as factors influencing material choice: service requirements, part design, material limitations, and economics. He described a situation where thermoplastic material is now being recommended over thermosetting; butyrate phones in color. Another reported advantage of the new phone is that it has an average injection cycle of under one minute.

Mr. Moylan felt the controversy as applied to his company's products involved urea and styrene in decorative housings. Urea, in his estimate, showed greater resistance to scratching and crazing, was self-extinguishing, did not collect electrostatic dust, and was resistant to cold flow.

A combination of properties should determine the final choice in Mr. Wolford's opinion. He distributed property charts on 15 plastics manufactured by Koppers as a handy reference tool. Low pressure-modified polyethylene will have a tremendous impact on the industry, he felt, and competing materials will be forced to undergo modifications and improvements. This represents the cycle of choice; a different material is produced and gains favor for a time until the old is modified. The important factor in competition should be that it upgrades plastics.

Table favors were supplied by Plastic Molding Powders, Inc., and door prizes compliments of Monsanto Chemical Co. and the Section.

A total of 27 new members was added by the New York Section during the past few months, as follows: George Mayurnik, Sales Manager, Aries Laboratories, Inc.; Peter D. Monti, Manager of Extrusion Dept., Mastercrafts Plastics Co., Inc.; Martin A. Olsen, Vice-President & Sales Manager, Olsen Mark Corp.; Charles S. Grey, President, Cee-Jay Extruders, Inc.; Edmund Greacen, Manager of Model Shop, Lippincott & Margulies Inc., Robert C. Magee, Works Manager, Thomson Industries Inc.; Walter E. Rayher, Sales Engineer, F. J. Stokes Machine Co.; Melvin Rosensaft, Production Manager, Ideal Toy Corp.; Nathan S. Moss, Productions Manager, Stafford Mfg. Co., Inc.; Donald M. Joseph, Sales Manager, Plastics Division, Ciba Company, Inc.; Dr. Ulrich Heubaum, Vice-President, Metachem, Inc.; Raymond S. Portner, Sales Rep., Ball & Jewel, Inc.; Frank C. Ludwig, Vice-President, Deoxygenation Corp.; Walter E. Morris, General Manager, Vizofilm Mfg. Corp.; Leroy A. Darling, Chemistry and Process Engineer, Sperry Gyroscope Co.; Lee Bodin, Plant Manager, Plastic Jewel Co.; J. Edward Brister, Dept. Head, Molding & Extrusion Div., Bakelite Co.; Joseph T. Bonanno, Supt. of Injection Molding Dept., Composition Novelty Co.; Joseph Pearlman, President, Kwik-Mold Plastic Corp.; Sanford J. Emil, Staff Engineer, Brilhart Plastics Corp.; George M. O'Brien, Asst. Manager of N.Y. Div., U. S. Industrial Chemicals Co., Div. of National Distillers Prod. Corp.; Harold A. Wasserman, Vice-President, Carlyle Industrial Corp.; Leonard Karmel, President, Carlyle Industrial Corp.; Arthur Basescu, President, Bassons Industries Corp.; Charles S. Stryker, Mgr. of Polyester Resin Development, Archer-Daniels-Midland Co.; James C. Bohlen, Director of Industrial Sales, Lunn Laminates, Inc.; George A. Flett, Eastern Area Manager, Rezolin, Inc.

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WHY MONKEYS OBJECT

Three monkeys were dining in a cocoanut tree
And were discussing some things about you and me.
Now what do you think? Now listen you two—
Here, monkeys is something that cannot be true
That humans descended from our noble race
Now that is ridiculous; a terrible disgrace.

Who ever heard of a monkey deserting his wife?
Or leaving his babies to starve, or even run "Rife"?
And have you ever heard of a dear mother monk
Leaving her darlings with strangers to "Bunk"?
Human babies are handed from one to another,
Some scarcely know the love of a mother.

And I've never known a monkey so selfish to be
That he would build a fence around a cocoanut tree,
So that other monkeys cannot get even a taste
But allow the cocoanuts to all go to waste.
Now if I'd put a fence around a cocoanut tree
I'd naturally expect you to steal them from me.

And there is another thing a monkey won't do
Get drunk on hard liquor, or get on a "stew"
Carouse and make whoopy, disgracing his life
Then reel madly home and beat up his wife.
They call this all pleasure, and make a big fuss
They're descended from something, BUT NEVER FROM US!

Bishop A. M. Johnson

P.S.—Any resemblance to persons in the Plastics Industry is an act of God for which Pribble Plastics Products, Inc., assumes no liability.

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WAYNE I. PRIBBLE

Buffalo

R. W. Miller

The Buffalo Section of the Society of Plastics Engineers met April 15, 1955 at the Park Lane Restaurant, Buffalo, New York. Approximately fifty members and guests gathered to hear Dr. V. T. Stannet, Associate Professor of the College of Forestry, State University of New York, speak on plastic-paper combinations.

Dr. Stannet, a graduate of the University of London, and the Polytechnic Institute of Brooklyn, presented a very interesting talk on the work that has been done at the University. Following are excerpts from Dr. Stannet's talk:

At Syracuse, they are primarily interested in the plastics to paper combination. Some interesting statistics are as follows: 1—20,000,000 lbs. of Urea Formaldehyde were added to paper, in 1954, in percentages of 0.5 -3.0. 2—12% of all polyethylene goes into paper. 3—Polyester resins added to paper give the advantage of continuous run of laminates. Prior to this development, it took high pressures to laminate. Now, with polyester, laminates can be made at fairly low pressure.

Dr. Stannet pointed out that all plastics can be added to paper. It is usually desired to obtain the following properties: 1—Resistance to gas and water vapor. 2—Resistance to water, liquids and grease. 3—Flexibility. 4—Heat and pressure sealability. 5—Chemical resistance. 6—Protection of printing. 7—Scuff resistance. 8 —Electrical insulation. 9—Structural strength. 10—Avoidance of fiber contamination.

It was pointed out that polyethylene has the tendency to absorb flavors and release them later. An alcoholic drink will lose all its flavor when stored in a polyethylene coated container. One mil of polystyrene between two layers of paper results in a product of exceptional dimensional for application in IBM cards. Milk cartons are now being coated with non-toxic resins and will probably replace the wax now being used.

Paper, filled with elastomers such as hycar or neoprene, are being made on paper machines. They have found use for shoe innersoles, and masking tape backings. Elastomer treated papers have the following properties: 1—Wet strength and other wet properties go up. 2—Elongation is better. 3 — Better dry strength. 4 — More puncture proof. 5 — Better chemical resistance. 6—Scuff resistance. 7—Tear and edge tear resistance. 8—Folding endurance. 9—Good sizing properties. 10—Good embossing and molding properties.

Dr. Stannet's talk indicates the field of plastic paper combinations is ever expanding, and will result in better products by the use of plastics.

Eastern New England

A. I. Simon

On May 19, 1955 the Eastern New England Section, SPE, held its monthly meeting at the Quartermaster Corps in Natick, Massachusetts. This was presided over by President Harry Connors of the B. F. Goodrich Company.

There was an excellent turnout with more than 130 present.

Joseph Clancy of Emery Industries, and chairman of the membership committee, introduced a number of new members present. He expressed confidence in having the goal of 200 members attained before the end of the current year.

Ralph L. Mondano of Paytheon Manufacturing Company gave the treasurer's report which included a $200.00 Lowell Technological scholarship obligation. As chairman of the educational and professional activities committee, he expressed the desire to see further funds appropriated for other such worthy educational projects.

SPE national director and SPE local president Richard Beckwith of Dow Chemical commented on his trip to Houston and other national SPE activities. He mentioned that publications of preprints of the SPE Atlantic City Conference have been donated to libraries of several universities.

After the usual drawings of the door prizes which were donated by Kopper Company and M. W. Kellogg Company, David Towler of the Kellogg Company and chairman of the program committee introduced Dr. Oesterling of the Natick Quartermaster Corps Research Labs. Dr. Oesterling gave an outline of the various groups at and the types of projects being undertaken by the Natick Quartermaster Corps Research Labs. He offered the facilities of the Laboratory's dining hall and auditorium for any future SPE meetings.

Dr. Murray, in charge of the Plastics Group at the Quartermaster Corps, presented some of the problems with which his group is working. Some of these included arctic plastic skis and snowshoes, and abrasion-resistant plastic dishware.

After the meeting, a very interesting guided tour was made of the various laboratories and facilities of the Quartermaster Corps in Natick.

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Miami Valley

M. V. Roode

The May meeting of the Miami Valley Section Society of Plastics Engineers was marked by a very informative, slide illustrated talk on "Coatings on Plastics" by guest speaker, Dr. Lloyd E. Parks, Vice President and General Manager of Logo Inc. of Chicago.

After a tasty swiss steak dinner at Schulyers Restaurant in Cincinnati, the 28 members and guests present heard Dr. Parks, introduced by our President, Joe Overwein, outline the problems involved in the formulation of pigmented coatings and in their application to various types of plastic materials.

Any pigmented coating for plastic parts consists essentially of two systems, resin and solvent, each of which must be chosen with great care and then tailored to the specific application, said Dr. Parks.

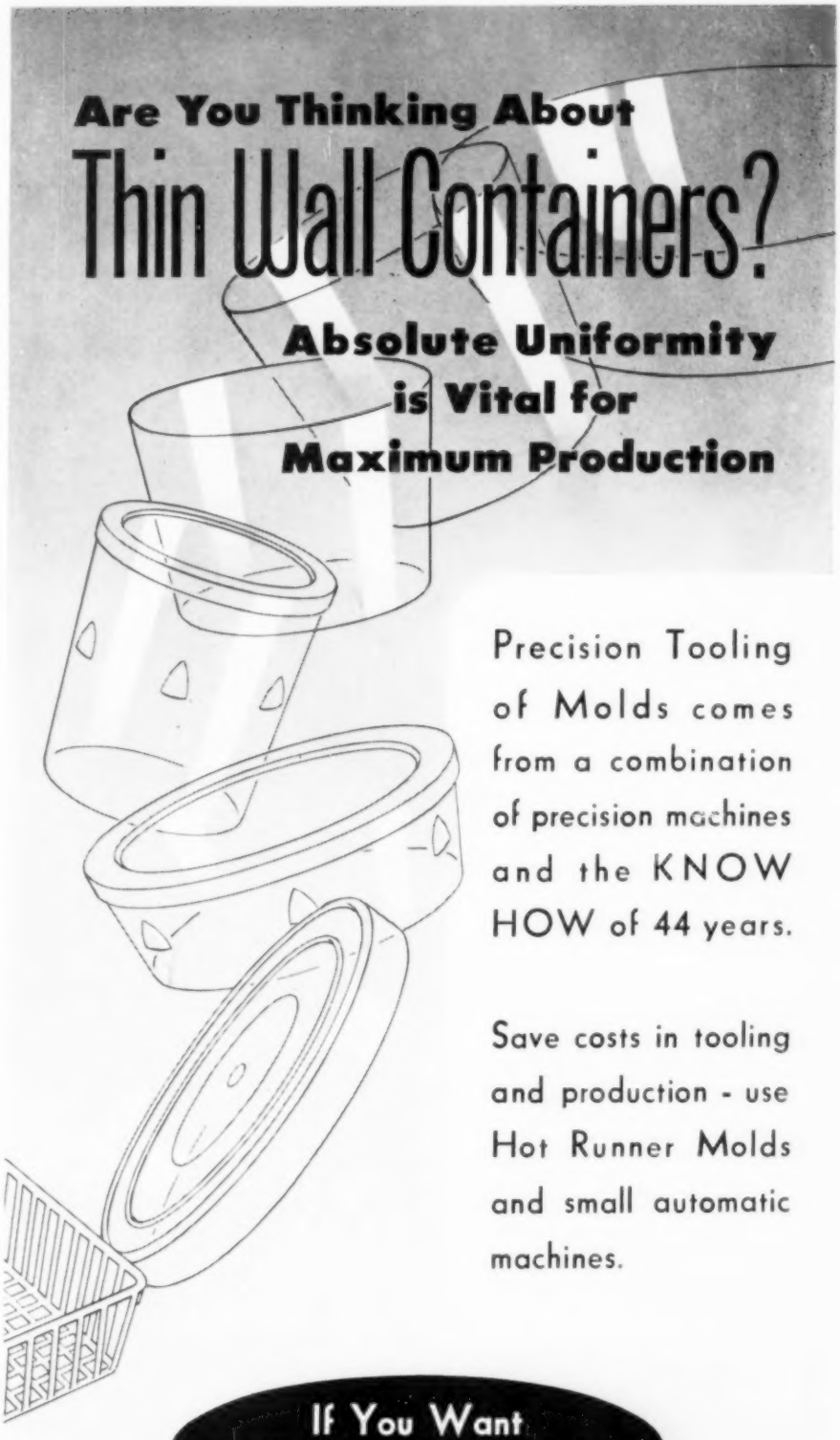
The requisite of a good resin system is its film forming characteristics, its pigment carrying ability, its flow, and its adhesion to the type plastic being coated.

Adhesion is probably the most fundamental property to be sought in a resin system. After good adhesion is obtained, the system may be modified with other resins to obtain other specific properties, although care must be exercised to retain the same quality of adhesion.

Equally important is the solvent system, necessary for the complete solvation of the resin system, yet which will not attack the plastic part to give crazing or etching. Other effects of improper balance of the solvents in the system may appear as orange peel, cratering or humidity blush in the applied coating.

Humidity blush may very easily occur on a wet day because the extra moisture in the atmosphere may affect the solvent balance to the extent that it will no longer hold the resin system in complete solvation. It is also possible, however, that in some instances humidity can be somewhat of an asset. High humidity may reduce the strength of a solvent in its attack on the plastic, thereby reducing crazing.

The method of application of the coating must also be considered in the formulation of the solvent system. A blend of high and low boiling solvents is used for spray coatings where a good portion of the low boiling solvent may be blown away in the spraying, thus increasing the solid contents and the viscosity of the wet paint film laid down. Yet, it is the high boiler that controls this viscosity of the wet film and must hold the resin in solution until the film is completely dry regardless of the method of application.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Toledo

D. E. Cordier

The May meeting of the Toledo Section was held at the new Toledo Express Airport on May 25th, with approximately 35 members and guests being present for dinner in the Airport dining room.

Following the dinner, the Section was privileged to enjoy a tour of the Airport control tower and an explanation of the various navigational devices and facilities in operation there.

New Section members were announced as follows: Mr. Don Basilius (Basilius Grinding and Tool), Mr. Charles Bellg (Plastics Tooling), Mr. John Larimore (Barrett Division), Mr. Carlton Richie (Owens-Illinois), Dr. Bobb Santelli (Owens-Illinois), Mr. Pete Swartzbaugh (Peerless Molded), and Mr. Al Tomaszewski (Peerless Molded). These accessions represent a definite step towards President Bowlin's stated goal of 80 members for the Toledo Section.

Mr. Jean F. Malone of B. F. Goodrich Chemical Company, was then introduced by Program Chairman Frank Rosenberger. Mr. Malone's subject was "Rigid Polyvinyl Chloride and Its Applications." Mr. Malone traced the history of rigid polyvinyl chloride from its commercial introduction in this country about 1950, through a usage of about 250,000 lbs. in 1952 and 2,500,000 lbs. in 1954, to an estimated 25,000,000 lbs. in 1955. This impressive performance has derived from a combination of outstanding chemical resistance, good physical and thermal properties, and satisfactory processing characteristics which have opened up opportunities as a structural material in the piping, building material and lighting fixture industries.

Engineering performance data relating to long time safe working stresses at 80°F. and 100°F. were presented to assist the designer in utilizing both Type I and Type II rigid PVC for structural uses.

In the building industry the use of rigid polyvinyl chloride for corrugated sheeting for internal partitions, opaque wall panelling and lighting fixtures was described. Rigid PVC corrugated sheeting is not recommended for external use because of the color degradation brought about by the ultra-violet component of ordinary sunlight. It should be noted, however, that one year Florida weathering tests on pipe failed to bring about any significant decrease in strength.

Mr. Malone presented the limitations of rigid PVC related to temperature range, service conditions and the specific materials to be handled, and also discussed the processing, fabrication and joining of the material.

A lively question-and-answer period concluded Mr. Malone's presentation of an interesting and significant new plastic material.

The summer golf meeting of SPE was held on June 29 at Chippewa Country Club at which time several members participated in golf and a putting contest.

A very short business meeting was conducted and the meeting was then turned over to Jim Hyland who was chairman of this particular outing.

Prizes were given for the various golf scores as well as attendance prizes at which time the meeting adjourned.

Milwaukee

Marvin C. Stowe

The Milwaukee Section had on June 11 what will be known from here on in as its Annual Summer Party. It was an all out affair with cocktails, dinner, dancing. Couples were encouraged which gave the party a very sociable atmosphere. Sixty members, guests, and wives attended.

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DETROIT

Mac Nichols

The 1955 "Spring Golf Gambol"—and you can spell it anyway you wish—was held on May 24th at the Plum Hollow Golf Club, and the Divot Diggers of the Detroit Section were out en masse.

The event, directed by Jay Kuhn the Program Chairman of the Chapter, was attended by 91 SPE members who brought with them 38 guests.

Seventy-three aspiring Sam Sneads started teeing off in the early afternoon and by dusk, the course looked like a plowed Iowa cornfield just prior to planting.

In the evening after the banquet, Pat Luckino, Chairman for the Day, presented the golf and door prizes. The Golf Kicker's Handicap was won by Russ Stanhope of Micromatic Hone and Mrs. Stanhope can go back and tell her nosy neighbors that it was not the Paddy Wagon that brought Hubby home—but a Brink's Armored car.

The MacDonald Manufacturing Company of New Baltimore donated a handsomely engraved Silver Bowl to be known as "The Stuart MacDonald Golf Trophy" which is to be given as a perpetual award to the Detroit Section golfing enthusiasts. Each year the low gross scorer in the Spring and Fall Tournaments will retain the trophy. Competition for "The Bowl" will be limited to SPE members of the Detroit Section.

Iver Freeman, the President of the Detroit Chapter, presented a plaid Nylon golf bag that was equipped with everything but oar locks, to Past-President Carl Whitlock as a token of the esteem and regard held for him by the Detroit Section during his term in office.

Effect of Fillers . . .

(Continued from page 46)

fillers such as powdered calcium carbonate, mica, asbestos, and wood flour. In general, fillers increase the shear modulus and decrease the damping over that of pure polystyrene. The heat generated by a unit deformation of the material, however, usually is increased by a filler. The results can be partially explained in terms of the individual mechanical properties of the polystyrene and filler and by an "adhesion factor."

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1. L. E. Nielsen, Rev. Sci. Instruments, 22, 690 (1951)
2. C. Zenzer, "Elasticity and Anealasticity of Metals," University of Chicago Press, 1948.

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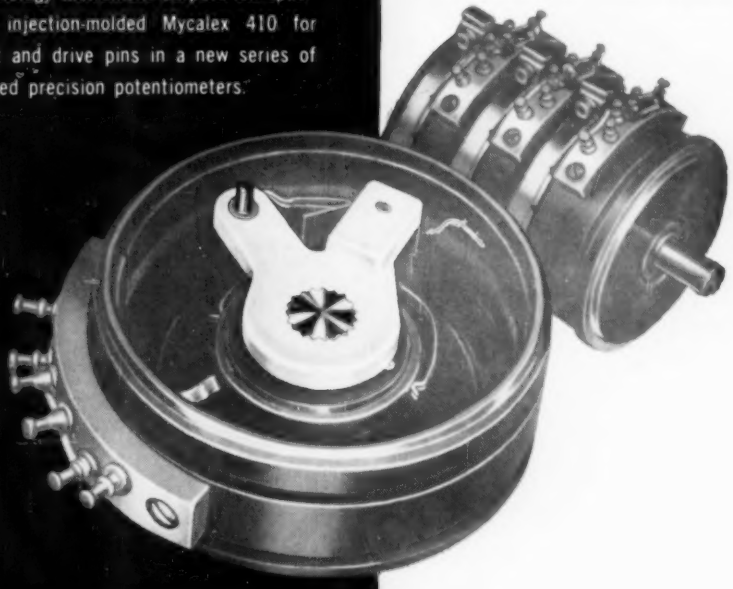

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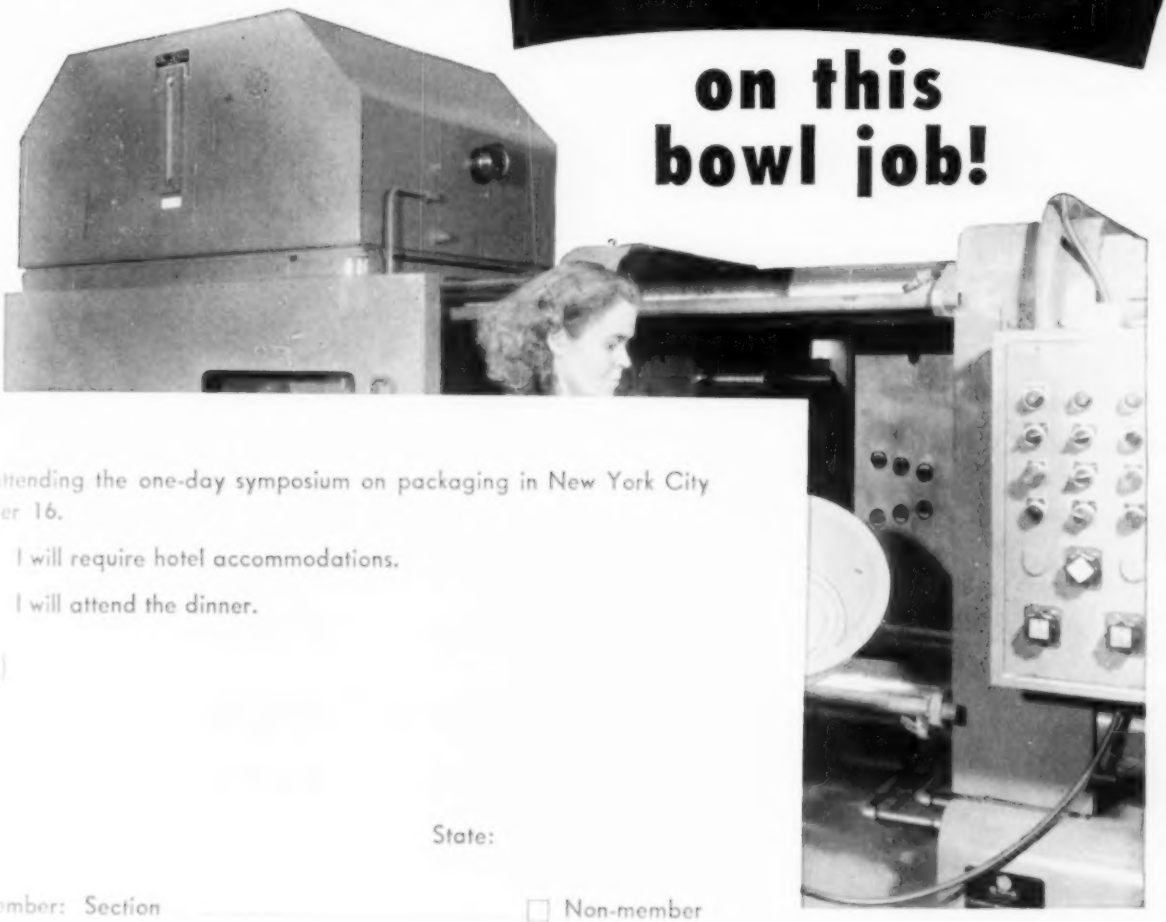

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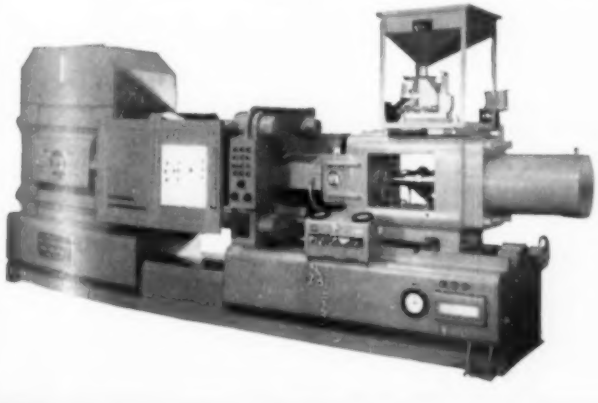

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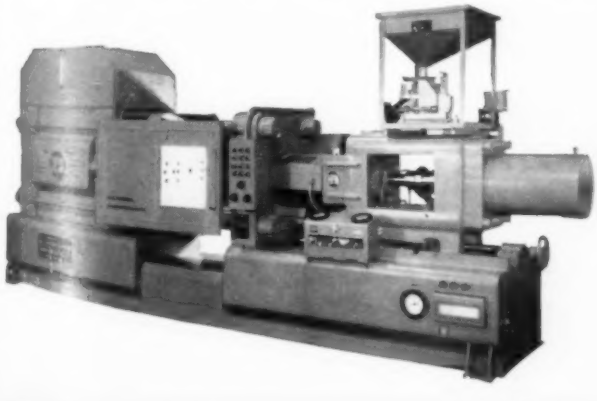

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